# A VALIDATION OF MATHEMATICAL MODELS
FOR TURBOJET TEST CELLS.

John Justin Walters
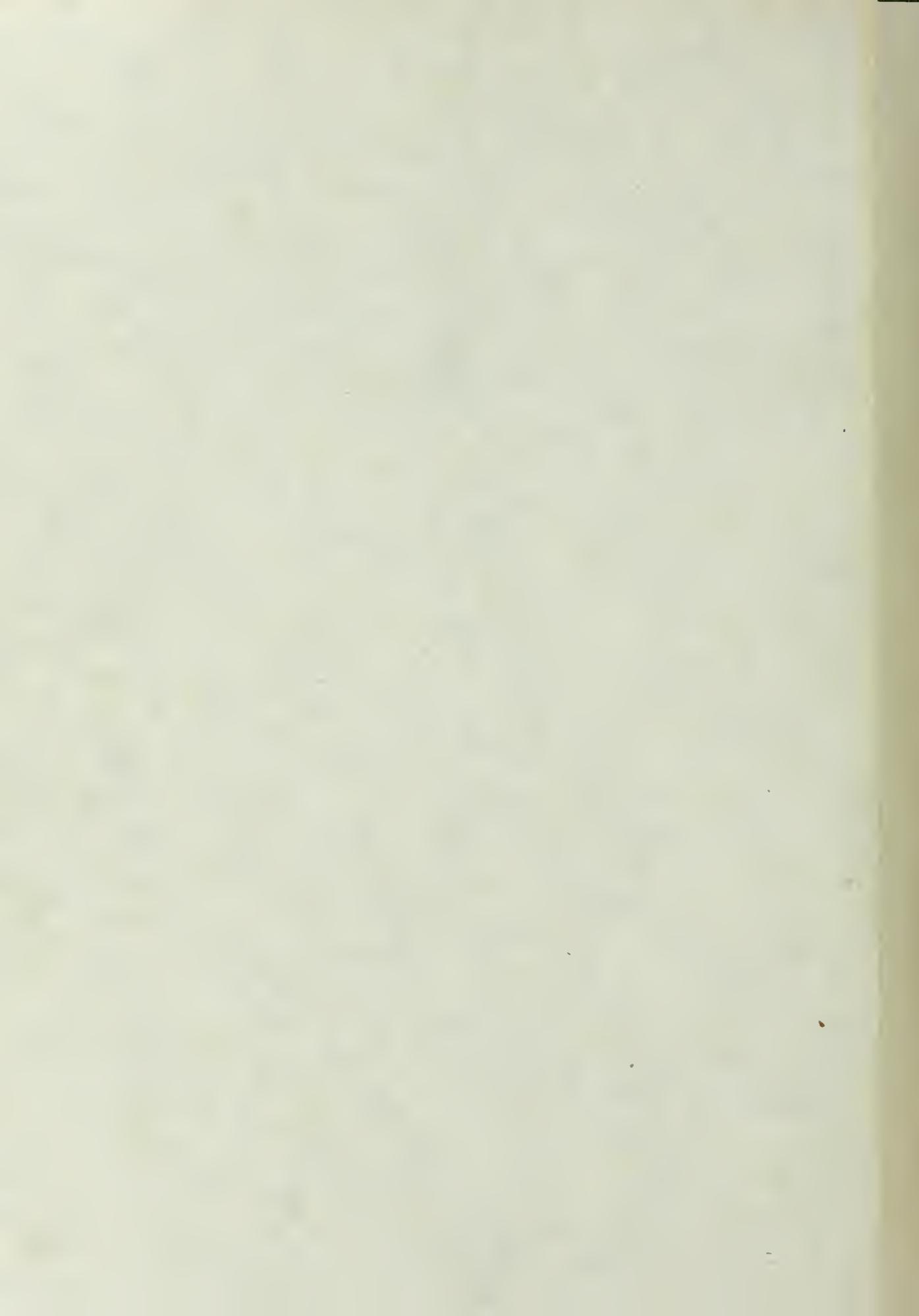
NAVAL POSTGRADUATE SCHOOL
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THESIS

A Validation of Mathematical Models for
Turbojet Test Cells

by

John Justin Walters

March 1978

Thesis Advisor: D. W. Netzer

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20. Abstract (continued)

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A Validation of Mathematical Models for
Turbojet Test Cells

by

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ABSTRACT

Previously developed one-dimensional and two-dimensional computer models for predicting turbojet test cell performance were compared with data obtained from a subscale test cell for the purpose of model validation. Comparisons were made for a variety of configurations and flow rates. A modified one-dimensional model was found to reasonably predict the variation of augmentation ratio with engine flow rate, although predicted magnitudes were consistently too small. The model incorporated excessive drag losses and an inaccurate jet spreading parameter for large engine-augmentor spacings. The two-dimensional model accurately predicted experimental velocity profiles, but overpredicted pressure variations, except for low engine exit Mach numbers.

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I. INTRODUCTION

Accurate, controlled testing of high technology jet
engines requires a fixed installation where such engines may
be operated throughout their full thrust envelope under
conditions approximating the installed situation, and with
sufficient instrumentation to assess performance parameters.
These installations, jet engine test cells, have taken many
forms, among them the "Hush-House" installed at NAS Miramar,
CA [Ref. 1] for installed engine testing, and a Coanda design
[Ref. 2] for noise suppression. The most common cell design
(Fig. 1) is typically a block house type installation with
vertical inlet and exhaust stacks configured for velocity
profile and noise control. Arrangements with horizontal
inlet and exhaust stacks exist and provide more uniform flow
profiles to the engine inlet, but the large clear areas
needed adjacent to this type cell precludes its frequent use.
Mounting hardware and monitoring equipment are provided as
appropriate to the engine under test.

Pollution control is a major problem in the operation
of cells with today's high power, high mass flow engines.
Noise and visible and invisible pollutants are emitted in
quantity. Judicial action initiated by the State of
California against Naval Air Station facilities in California
has brought these problems into prominence. Atmospheric
pollution has been attacked using various forms of water

droplet adhesion, mechanical grid entrapment, or electronic ionization, with baffle combinations for acoustic treatment [Ref. 3]. Good results have been obtained at NAS Jacksonville, FL, with a water scrubbing technique to remove pollutant particles. All these methods are expensive and complicated, however, and simpler ones are desired. To find them will require a detailed understanding of test cell aerodynamics.

In a basic arrangement (Fig. 1) the engine is situated somewhere near the center of the cell to allow a near uniform flow profile to develop. It exhausts into an augmentor tube where more air is entrained from within the cell, causing air to flow around the engine into the augmentor tube. The ratio of entrained air mass flow rate to engine mass flow rate is known as "augmentation ratio." The entrained "secondary air" acts as a coolant for the hot engine gases, extending exhaust stack life, and as a diluent for the exhaust products. Augmentor parameters such as engine-augmentor spacing and augmentor diameter are important to proper cell/engine operation due to their effect on augmentation ratio. An excessively high augmentation ratio (high secondary air flow) may cause large pressure gradients between engine inlet and exhaust planes with resulting inaccurate performance measurements. In addition, test cell structural limits might be exceeded due to excessive cell pressure reduction. Insufficient secondary air could allow hot exhaust gas recirculation to the engine inlet with

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performance degradation as well as hot spots in the augmentor tube and exhaust stacks. Insufficient secondary air flow also causes excessive density of visible emittants. This may violate Ringleman Number limitations of local pollution ordinances even when the pollutant output quantities are within specified limitations.

Testing for optimization of engine-augmentor relationships cannot easily be accomplished in existing, operational test cells. Cells are scheduled to maximum capacity for economy reasons, making test runs inconvenient. Also, today's large engines and soaring fuel costs make full scale testing prohibitively expensive. Clearly, computer modeling of flows provides a possible alternative.

A working, proven model would be able to predict augmentation ratios and recirculation of hot engine exhaust gases as a function of augmentor design. It could provide information on the optimal location for water quenching devices needed to cool hot afterburner exhaust flows and save expensive, time consuming, trial and error procedures now used. With these capabilities, a model would allow a more optimal cell design process than is now possible.

A number of models with some or all of these capabilities are in existence. One by United Research Laboratories [Ref. 4] uses a one-dimensional idealization combined with empirical correlation factors to adjust output values of exit static and total pressure (to agree with experimental results), then generates performance data for various

augmentor combinations. Ellin and Pucci [Ref. 5] attacked a similar problem in modeling a gas eductor system for gas turbine powered ships. Due to differences in application, however, this model made no allowances for spacing between nozzle exhaust and tube inlet, a commonly found configuration in test cells. Bailey [Ref. 6] also used the one-dimensional idealization and accounted for friction and inlet losses through empirical data and engine-augmentor spacing by means of theoretical spreading equations [Ref. 7] for the engine exhaust stream. The problem was also approached using two-dimensional theory by Hayes and Netzer [Ref. 8], who solved elliptic flow equations.

Experimental validation of the last two models was the subject of this study. The one-dimensional Bailey model is simple and uses little computer time. It relies heavily on empirical loss factors which are applied to flow conditions different from the original experiments. It is fundamentally restricted to calculation of trends of augmentation ratio with variations in engine flow rate, engine augmentor spacing, engine diameter, and augmentor diameter. It has, on the other hand, no restrictions to conditions of application. It incorporates corrections for choked flow at the engine nozzle and the basic one-dimensional equations for mass, energy, and momentum conservation are not limited by Mach number. It does not, however, incorporate provisions for handling exhaust shocks, or jet spreading for choked flow conditions.

The two-dimensional model supplies a far more complete data set. It predicts temperatures, velocities, pressures, and turbulence levels throughout the flow field within the augmentor tube and parts of the test cell. It is a more rigorous solution of the basic conservation equations and relies on empirical constants only for the turbulence modeling and boundary conditions on vorticity. It is limited by an assumption of incompressible flow (more accurately, density is assumed to vary with temperature and composition but not with pressure) which prevents accurate results above flow Mach numbers of about 0.6. It is additionally weakened by the use of stream function and vorticity as primary variables. These variables make the solution simpler by eliminating pressure from the equations and by reducing the dependence on velocities. However, the recovery of pressure from the solution is extremely sensitive to calculations for the stream functions. Both these areas can be strengthened by new computation techniques. These include elliptic equations which reduce to parabolic in appropriate conditions, allowing calculations to flow Mach numbers of 1.0, and the use of pressure and temperature as primary variables. These techniques are currently being investigated at the Naval Postgraduate School.

All test cell models in existence which are available in the open literature have the same basic drawback -- none have been experimentally validated over the normal range of test conditions found in actual test cells. Due to the wide

range of assumptions incorporated in the models, and the unusual flow conditions found in jet engine exhaust (hot, high velocity core entraining cold, stationary air), models must be tested against experimental data before they can be used with any confidence. Testing should include measurement of velocity profiles in the test cell and augmentor tube to test the validity of inlet and exit profile assumptions made in all models and the accuracy of flow field velocity computations in the two-dimensional models. Accurate measurements of flowrates and augmentation ratio are necessary to provide inputs to the 2-D model and to check predicted results in the 1-D model. Pressures and temperatures along the augmentor tube and in the test cell must be measured to provide data for comparison to 2-D predictions.

To make all these measurements in a full scale operational cell would be time consuming and costly for the same reasons mentioned earlier. For these reasons, the subscale test cell designed and built at the Naval Postgraduate School [Ref. 9] was chosen for validation efforts. It is substantially less expensive and more convenient to operate and can easily and inexpensively be completely instrumented. Configurations can be quickly varied and data easily and rapidly collected and reduced.

II. <u>METHOD OF INVESTIGATION</u>

A previously constructed one-eighth scale turbojet test cell [Ref. 9] was modified to increase quantity of obtainable data, and used in a validation study of two test cell flow models. A one-dimensional and a two-dimensional model were evaluated. The cell was used to determine the effects of engine-augmentor spacing, engine mass flow rate, engine nozzle total temperature and augmentor inlet geometry on augmentation ratio and augmentor pressure, velocity, and temperature distributions. Experimentally measured pressure, temperature, and velocity profiles were compared with theoretical predictions.

III. EXPERIMENTAL APPARATUS

A. TEST CELL

Design and construction of the subscale turbojet test cell are detailed in Ref. 9 and shown in Figs. 2 and 3. The cell is a one-eighth scale model of a NAS Alameda test cell. A TF41 engine was scaled to one-eighth in diameter resulting in mass flow being scaled by 1/64 to maintain flow velocities equal to those in the full scale cell. Air was drawn into the test section through a horizontal inlet with square bellmouth and a flow straightening section. The test section was enclosed by hinged plexiglass sides to allow easy access and visual monitoring of the section during operation. The augmentor tube, equipped for interchangeable inlets, exited the cell through a removable wall. Its downstream end was attached to a deflector-plate-equipped vertical exhaust stack. The stack was mounted on a wheel/ rail arrangement which allowed translation of the stack/ augmentor assembly and resulting changes in spacing between the engine and augmentor tube. Removable metal grates were installed in the stack to permit variation in back pressure.

The engine used to simulate turbojet/turbofan tailpipe and nozzle conditions was a forced air ramjet supplied with compressed air from an Allis-Chalmers, twelve-stage axial compressor. Separate three-inch pressure lines supplied variable combustor (primary) and bypass (secondary) air.

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The engine intake was simulated by a variable suction
six-inch line drawing through a six-inch bellmouth. The
combustor was a sudden expansion (or dump) type fed by
nitrogen-pressurized JP4 and ignited by a methane-oxygen
torch in the combustor wall. The arrangement allowed control
of simulated tailpipe flow rate, nozzle total temperature
and pressure, and nozzle geometry.

B.   ENGINE AND AUGMENTOR

Figure 4 shows the basic placement of the ramjet in the
test section when viewed from downstream. Figure 5 shows
the geometry of the engine-augmentor interface. Mass flow
rate could be controlled independently in the primary and
secondary lines, providing a simulation of a variable bypass
turbofan, if desired. Mixing of the primary and secondary
streams was, unfortunately, not as complete as in an actual
turbofan and in hot runs the arrangement resulted in a hot
inner core of primary air surrounded and pinched by cold
secondary air. Lengthening of the mixing section will
eliminate this problem in the future. Engine-augmentor
spacing was varied by rolling the exhaust stack along its
rails. The three augmentor tube inlets primarily tested
(Fig. 6) were separate pieces, easily changed by the removal
of two screws.

C. INSTRUMENTATION

Flow rates in the three-inch primary and secondary and six-inch suction lines were measured using ASME type flow orifices and controlled by hand valves. Pressure taps and thermocouples in the test cell allowed monitoring of pressures at the engine inlet and exhaust planes and ambient air temperatures. A pressure tap and iron-constantan thermocouple in the combustor outer wall allowed calculation of nozzle total temperature and pressure. Augmentor flow conditions were monitored by 27 pressure taps along the top of the tube, spaced at one-inch intervals near the upstream and downstream ends, spreading to four inches near the center. Twelve copper-constantan thermocouples were also placed along the tube at four-inch intervals. All thermocouples were referenced against an ice water bath. Velocity profiles in the augmentor were measured with a pitot rake consisting of seven equally spaced small diameter total pressure tubes. The rake could be rotated and translated to obtain vertical and horizontal profiles over the full length of the tube. A thermocouple was also mounted on the center pitot tube. Velocity profiles in the cell were measured by means of a direct reading anemometer with hot wire probe. The probe had been extended sufficiently to allow measurements across the entire height and width of the cell.

D.   DATA ACQUISITION   19

The data acquisition system consisted of a fully
programmable Hewlett-Packard desk top calculator with a
HP-9867B Mass Memory, HP-9862A Plotter, and digital tape
reader. A B. and F. Model SY133 data logger scanned 48
pressure and 20 temperature channels per run and punched
raw data onto a paper tape for reading into the Hewlett-
Packard system. The data reduction system produced reduced
flow rates, augmentation ratio, temperatures, and pressures
and automatically plotted augmentor pressure and temperature
profiles.

IV.   EXPERIMENTAL PROCEDURES

A.   TEST PROCEDURES

Initial cell startup required about one hour for warmup of the Allis-Chalmers compressor. Once air was directed through the lines, ten to fifteen minutes were required for temperature stabilization at the flow orifices before accurate flow rates could be set. Once the desired mechanical configuration of the cell was fixed, flow rates were adjusted. Rough adjustments were made using water manometers located at the hand valves. Fine adjustments were made by running flow rate data through the data reduction system and checking calculated values. When the proper values were attained, a complete set of data was processed by setting the B. & F. data logger to scan the 40 pressure and 20 temperature channels. The raw data was recorded on punched tape. The tape was then read into the data reduction system for a complete data readout. When cell velocity profiles were desired, they were obtained by manually reading and recording the probe measurements as it was inserted through the side or top of the cell. The pitot rake was also repositioned manually when velocity profiles at more than one tube position were measured.

B. DATA REDUCTION

Existing programs for the HP9830A calculator were modified to handle increased data quantity and extract more information from the raw data. Flow rates were calculated using ASME equations for D-.5D orifice pressure tap configurations. Temperatures were obtained by curve-fitting published thermocouple data. Pitot rake pressures were combined with tube wall pressure and temperature measurements to calculate the velocity profile at each position of the rake. This velocity profile was then integrated by Simpson's rule to find the average flow velocity in the tube. The velocity profile at the aft end of the augmentor tube was quite flat and varied only slightly in the tangential direction. For all tests conducted, the average velocity at this location was used to determine the flow rate through the augmentor tube. This flow rate, together with the measured engine flow rate, allowed calculation of the augmentation ratio. During the initial phase of the investigation, the augmentation ratio determined in the above manner was validated against the value determined from the test cell velocity profile. A routine was included in the program to automatically plot tube pressure and temperature profiles when desired.

C.   TEST CONDITIONS

   Data were taken for the set of test conditions
presented in Table 1.   The bellmouth data were used for 1-D
model validation and the flat and straight augmentor inlet
data were used for 2-D model validation.

<div align="center">TABLE I</div>

<div align="center">TEST CONDITIONS</div>

INLET	NOZZLE FLOW RATE (lb_m/sec)	ENG-AUG SPACING	ENG-INLET SPACING
Bellmouth	1.0 1.5 2.0 2.3	0	-1.12D
	1.0 1.5 2.0 2.3	1.12D	0
	1.0 1.5 2.0 2.3	2.12D	1D
	1.0 1.5 2.0 2.3	3.12D	2D
Flat	1.0 1.0 (Hot) 1.5	0	
	1.0 1.0 (Hot) 1.5	1D	
	1.0 1.0 (Hot) 1.5	2D	
Straight	1.0 1.5	0	

V. MODELS TESTED

A. ONE-DIMENSIONAL MODEL

The one-dimensional model tested was described by Bailey in Ref. 6. Geometry is shown in Figure 7.

The model assumptions were:

1. one-dimensional, steady
2. adiabatic
3. flow uniform at cross sections 1, 2, and 3
4. all gases ideal
5. mixing occurs in a constant area
6. $P_1 = P_2$
7. $A_1 + A_2 = A_3$

Values of tube and engine nozzle diameters, nozzle total temperature and pressure, and secondary air total temperature were input. A value of nozzle exit static pressure (P_1) was set, which set P_2, and bypass air total pressure ($P_{T_{2_a}}$) was assumed as atmospheric. Initially, P_{T_2} was set equal to $P_{T_{2_a}}$. From these, the model calculated the Mach numbers, and velocities at 1 and 2. The loss across the augmentor inlet was then calculated using an empirical loss constant for the particular inlet being employed. This resulted in a new value of P_{T_2} and the process was repeated until successive changes in P_{T_2} became small. Primary (1) and secondary (2) mass flow rates were calculated and used to solve the one-dimensional equations of momentum and energy between 1/2 and 3.

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Spreading of a subsonic jet with engine augmentor spacings greater than zero was handled by use of a spreading parameter to determine the size of the mixing zone at the entrance to the augmentor tube. Velocity profiles within the mixing zone were modeled by an error function. The mixing zone was broken into a large number of small areas and then properties in each were calculated and summed to solve the one-dimensional equations. In the case of choked flow (for 1-D isentropic conditions) in the nozzle, a correction for choking was applied to the nozzle flow rate and, for the zero spacing case, the problem was handled as before. No attempt was made in the original model to handle the choked flow case at spacings greater than zero.

Augmentor tube wall friction in the model was approximated using an equation for a flat plate drag coefficient. The drag coefficient was calculated using a Reynold's Number based on augmentor length and $\bar{V} = (V_3 + V_2)/2$ and $\bar{T} = (T_3 + T_2)/2$ (for determination of viscosity). Although not mentioned in the text of Ref. 6, an empirical coefficient was found in the equation in the program listing which arbitrarily reduced the wall friction value. The coefficient proved essential to successful operation of the model since without it, the calculated flow resistance was so high that reverse flow was predicted in the tube for all cases tested. The use of the lower value of wall friction apparently is necessary because of the considerable difference in flat

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plate velocity profiles from the developing coaxial flow within the augmentor tube.

To use the model as written, values of nozzle total temperature and pressure and secondary air total temperature are input with geometries and the head loss factor for the tube inlet. The published listing was modified slightly to allow entering the experimentally obtained value of back pressure (P_3). In the original model, solutions were obtained for a number of arbitrarily set values. The model then calculates primary and secondary flow rates and augmentation ratio. It is simple to use and requires minimal computing time. At the same time, it depends heavily on empirical constants for inlet losses and wall friction and the "plug flow" assumption is questionable in the case of high velocity entrance flows. It provides insight only into overall behavior, with little indication of behavior within the tube and no information on flow patterns in the test cell itself.

The model had previously been tested in a 1/24th scale plexiglass cell [Refs. 6 and 10]. Bailey found the model accurately predicted the effects of nozzle total pressure on augmentation ratio and the variation of secondary mass flow with primary mass flow. The region of choked nozzle flow was avoided since no provision was made to compute the spread of a supersonic stream and no valid calculation could be made for other than zero spacing. He discovered that published head loss factors caused excessive inlet losses and

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predictions were most accurate with a loss factor of essentially zero. No conclusive results were obtained for tests of engine-augmentor spacing effects on augmentation ratio due to widely scattered experimental data. Published comparisons were made with no inlet on the augmentor tube.

B.   TWO-DIMENSIONAL MODEL

The two-dimensional model tested was described by Hayes and Netzer in Ref. 8. The model solved the basic equations of mass, momentum, and energy in two-dimensional, elliptic form. Stream function and vorticity were the primary variables for mass and momentum effects which reduced coupling problems and made equation solution easier. Temperature was the dependent variable for energy conservation. Kinetic heating was ignored and specific heat was considered constant. Turbulence effects were included in the form of a two parameter model describing the effective viscosity. The relationship used was:

$$\mu_{eff} = C_\mu \frac{\rho K^2}{\varepsilon}$$

where:

μ_{eff} = effective viscosity

C_μ = empirical coefficient

ρ = local density

K = turbulence kinetic energy

ε = turbulence energy dissipation rate

The equations for K and ϵ were written in elliptic form and the five equations were solved with the perfect gas law to describe the flow field. Once the solution converged, axial and radial pressure distributions were obtained from the stream function distribution.

The equations were solved using point-by-point Gauss-Siedel with under-relaxation. The model was assumed axisymmetric, and described by a 43 by 40 grid in cylindrical coordinates. A flat augmentor inlet lip (Fig. 6) was included in the model as an approximation to the bevelled inlets found in some cells at NARF Alameda. The straight pipe was approximated by making this lip very small.

The model had several limitations. The elliptic equations using stream function and vorticity are inherently limited to low subsonic speeds. In addition, the use of stream function and vorticity as primary variables made calculated pressures extremely sensitive to small errors in convergence. The rectangular grid system does not allow modeling of rounded, bellmouth type inlets commonly found in operation. Solution required an average of 170 minutes CPU time on the school's IBM 360-67 computer. On the plus side, it is a potentially valuable tool for predicting effects of engine-augmentor spacing, augmentor diameter, and cell geometry on pressure, velocity, and temperature distributions within the test cell and augmentor at low engine flow settings. This information is needed for optimization of noise suppression and chemical pollution abatement techniques.

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VI. <u>RESULTS AND DISCUSSION</u>

A. ONE-DIMENSIONAL MODEL

As discussed above, the model developed by Bailey was not applicable for choked nozzle flow since it did not account for any jet spreading or shocks. In the model, choking was assumed to occur for the one-dimensional isentropic pressure ratio (γ = 1.4) of 1.89. Converging nozzles with 25° half angles (as used in these experiments) do not actually choke until nozzle pressure ratios in excess of 2.5 [Ref. 11] are obtained. This results from three-dimensional effects (radial momentum, etc.) which cause the lines of constant Mach number to be convex in shape as the flow exits the nozzle. In the experiments conducted in this investigation, pressure ratios did not exceed 2.4. For these pressure ratios, the flow leaving the nozzle would continue to accelerate, with resulting weak shock patterns. Since the shocks are quite weak for these low pressure ratios, a good approximation for the jet behavior may be to neglect the shocks entirely and assume that the jet continues to accelerate until the jet pressure equals the local ambient pressure a short distance from the nozzle exit plane. The jet could then be considered to spread in the same manner as employed by Bailey for the subsonic jet. This modification was incorporated into the 1-D model.

The original model generally predicted gross trends in cell performance correctly, although with some severe limitations. Effects of engine-augmentor spacing on augmentation ratio are shown in Figs. 8 and 9 for the test cell and model, respectively. Experimentally, cell augmentation ratio was found to decrease slightly and then level off with increased spacing (Fig. 8). The level off at increased spacing seems to indicate that the jet ceased spreading at a point about two nozzle diameters downstream. The decrease found at zero spacing probably resulted from flow interference by the mounting flange of the engine nozzle which, at these separations, was very near the tube inlet. The model, which predicts continued jet spread for any distance from the nozzle, did not predict the leveling off noted in the test cell and could not predict the "hump" caused by flange blockage. It did predict the correct trends for spacings between about one and two nozzle diameters. However, the predicted augmentation ratios were consistently lower than the experimental values. In the original model, choking was assumed to occur at the 1-D critical pressure ratio which would have caused the 2.0 and 2.2 lb_m/sec. cases in Figure 9 to be choked.

The model also predicted the effect of changing flow rate on augmentation ratio correctly. Figure 10 shows that augmentation ratio decreased steadily with increasing flow rate in the test cell and Fig. 11 shows the same trend for

the model. Predicted augmentation ratios were lower than those experimentally measured. This was probably due to the excessively high drag calculated for the tube, which would cause a decrease in secondary air flow for a fixed primary air flow rate. The difference may also be the result of more entrainment in the jet spreading zone of the test cell than is accounted for in the model. The fact that the curves do not all descend with increasing spacing in Figure 10 as they do in Figure 11 reflects the "hump" noted earlier in Figure 8 which has caused the zero and 1.12D spacing lines to reverse in order.

Figures 12 through 15 compare augmentation ratio vs flow rate for model and experiment. As noted earlier, trends were predicted correctly although calculated values were substantially lower than those measured experimentally. The fact that the disparity increased with increased spacing again reflects the inability of the model to accurately predict spreading at distances greater than about 2 nozzle diameters.

The four model points shown at a 1.3 lb_m/sec. flow rate for the maximum spacing case, (Fig. 15) are based on estimated data since, for the experimental 1.0 lb_m/sec. flow rate, the model predicted reverse flow caused by excessive back pressure. This again indicates that the model incorporates losses that are too large since, in the momentum equation, an increase in drag has much the same effect as an increase in back pressure. The effect of varying back pressure in the model on augmentation ratio is shown in Fig. 15. It is seen that model

predictions are quite sensitive to the back pressure. Roughly, a change of 0.1% in back pressure changes augmentation ratio about 3%.

In general, results agreed with those found by Bailey for subsonic flow and showed the same trends when the original assumptions were applied to conditions which would be choked in 1-D calculations. The model basically predicted trends accurately. The major problems encountered were excessive drag losses and a failure of the spreading approximation beyond 2 nozzle diameters.

B. TWO-DIMENSIONAL MODEL

Predictions using this model were generally very accurate when it was operated within its design limitations. Elliptic type equations using the stream function-vorticity variables are generally unreliable for high subsonic velocities, since density is not considered to vary with pressure (which has been removed from the equations). Model flow rates of 1.0 lb_m/sec. resulted in a nozzle exit Mach of about 0.57 in cold flow. The higher flow condition at 1.5 lb_m/sec. produced an exit Mach of about 0.86. Difficulty was experienced in running comparisons of the 1.0 flow rate when high nozzle total temperatures (hot flow) were employed. The model requires nozzle exit static temperature as an input parameter. An accurate value for this temperature was impossible to determine from the experiments since the exhaust jet consisted of a hot inner core at greater than

2000°R surrounded by a cool blanket of near ambient secondary air. After some unsatisfactory trial and error, an average exit temperature of 900°R was assumed for all hot runs. This corresponded to an exit Mach of about 0.8.

Velocity profile predictions for cold flow in the augmentor were in good agreement with experiment (Figs. 16 through 19). The model does seem to consistently predict a slightly slower flattening of the profile than actually occurred since it consistently overestimated peak velocities far downstream and underpredicted close to the nozzle. The least accurate prediction was at the intake lip for the zero spacing case (Fig. 16). This is most likely due to the extreme blockage of the flow at the tube lip in the experimental setup. Since the calculated velocity must be extracted from stream function values, any error in calculation of the streamlines in this rapidly converging region would be somewhat amplified in the velocity calculation. As anticipated, prediction error was slightly greater in the high flow rate case (Fig. 19). This was not surprising since "pinching" of the nozzle flow caused a predicted centerline acceleration to a flow Mach number of 1.12, about 2.5 nozzle diameters down the augmentor. Surprisingly, in this case, the model predicted peak velocity at the tube inlet to within 3%.

Pressure predictions (Figs. 20 through 28) were in relatively good agreement with experiment, although not so

impressive as the velocity results. Pressure profiles were calculated along 5 different axial grid lines of the tube, ranging in position from near centerline to about mid-radius. The outermost lines were just inside the edge of the lip of the flat intake. The line J=17 (the second outermost) was found most accurate for cases with the lip installed while J=18, (slightly farther out) better fit those cases with no inlet (Figs. 27 and 28). The better accuracy is found to occur along axial lines that penetrate the upstream flow in the least disturbed (more axial flow) region. In addition, accuracy is generally good at the low flow rates but degrades markedly at higher mass flows. These results were expected since the need to extract pressure field information from the streamline solution through two successive approximate derivative calculations renders pressures extremely sensitive to small errors in the streamlines. This sensitivity is especially evident in the figures where the pressure drop in the entrance section of the augmentor tube is consistently over predicted, the error being more significant for the high flow rates.

The model proved ineffective in predicting hot flow results. An example (Fig. 26) shows the standard result -- a grossly excessive pressure drop accompanied by insufficient rise to match downstream values. Analysis of model predictions showed the nozzle flow had accelerated from a Mach of 0.8 at the nozzle exit to a value greater than Mach 1.2 along the centerline, a flow region where the model is not valid.

Results were similarly poor for hot flow temperature predictions, (Fig. 29) where data was non-dimensionalized using the maximum temperature of the run to compensate for the recognized inaccuracy of input flow temperature. While the model consistently predicted gradual mixing with maximum tube wall temperature at the downstream end, the experimental data consistently showed peak temperature at about one-third the length of the tube from the upstream end. The discrepancy in these temperature profiles is probably attributable to both the high flow Mach numbers and the poorly defined experimental nozzle exit temperature and velocity distributions as discussed above. In the cold flow case (Fig. 30), agreement was excellent.

VII. CONCLUSIONS AND RECOMMENDATIONS

A. ONE-DIMENSIONAL MODEL

The model tested has several advantages over the more elaborate 2-D version. It used little computer time and was, therefore, very significantly less expensive to operate. It also was applicable over a wider range of conditions, not being inherently limited to low subsonic speeds. The model was not capable of accurate quantitative predictions in any flow condition, but, with an assumption of no physical choking at the nozzle exit and only weak shocks, it predicted trends accurately up to flow Mach numbers of about 1.2. The model could probably be made to closely fit experimental data by trial and error adjustments of empirical constants and then be used to consistently predict conditions for a given installation. The heavy dependence on empirical data in the model weakens its potential value as a design tool since required constants may change from installation to installation. Loss factors for unusual configurations, such as the bevelled augmentor inlets found at NARF Alameda, could only be determined by additional experimental measurements.

The subscale test cell utilized in this project can potentially be used to alleviate this shortcoming by providing an inexpensive and convenient test device to establish data for virtually any conceivable situation. It will, however, require validation of at least some selected

conditions against full-scale test cell data, since Reynold's number differences that exist could alter test results.

In short, before the one-dimensional model could be applied as a workable tool, it would require more extensive testing, either against the subscale cell or a full-scale cell, to more accurately determine actual losses in the augmentor and the inlet. It may also require some more detailed method of handling the choked flow region than is now incorporated and an improvement in the spreading model, to handle engine-augmentor spacings greater than two nozzle diameters. One such approximation would be to consider the jet to remain fixed in diameter after two nozzle diameters of spreading. A detailed future study of the model using the subscale model over a wider range of conditions might provide the basis for these adjustments.

B. TWO-DIMENSIONAL MODEL

This model is inherently limited to low subsonic flow conditions, a situation which renders it unusable in its present form as a design tool for test cells handling today's afterburning engines. Its quantitative accuracy, in its limited flow region, is very good when predicting velocities in the augmentor tube, but somewhat weaker when predicting pressures due to the need to extract them from stream function information. The model used a large amount of computing time but provided an enormous amount of detailed information on cell performance, a valuable asset in a design tool.

The model's greatest value probably lies in its function as a stepping stone to more advanced 2-D models. Current work is being directed toward a model which utilizes velocities and pressure as the primary variables. This model should improve both the accuracy and utility of the 2-D model and should provide reasonable solutions for the choked flow operating conditions.
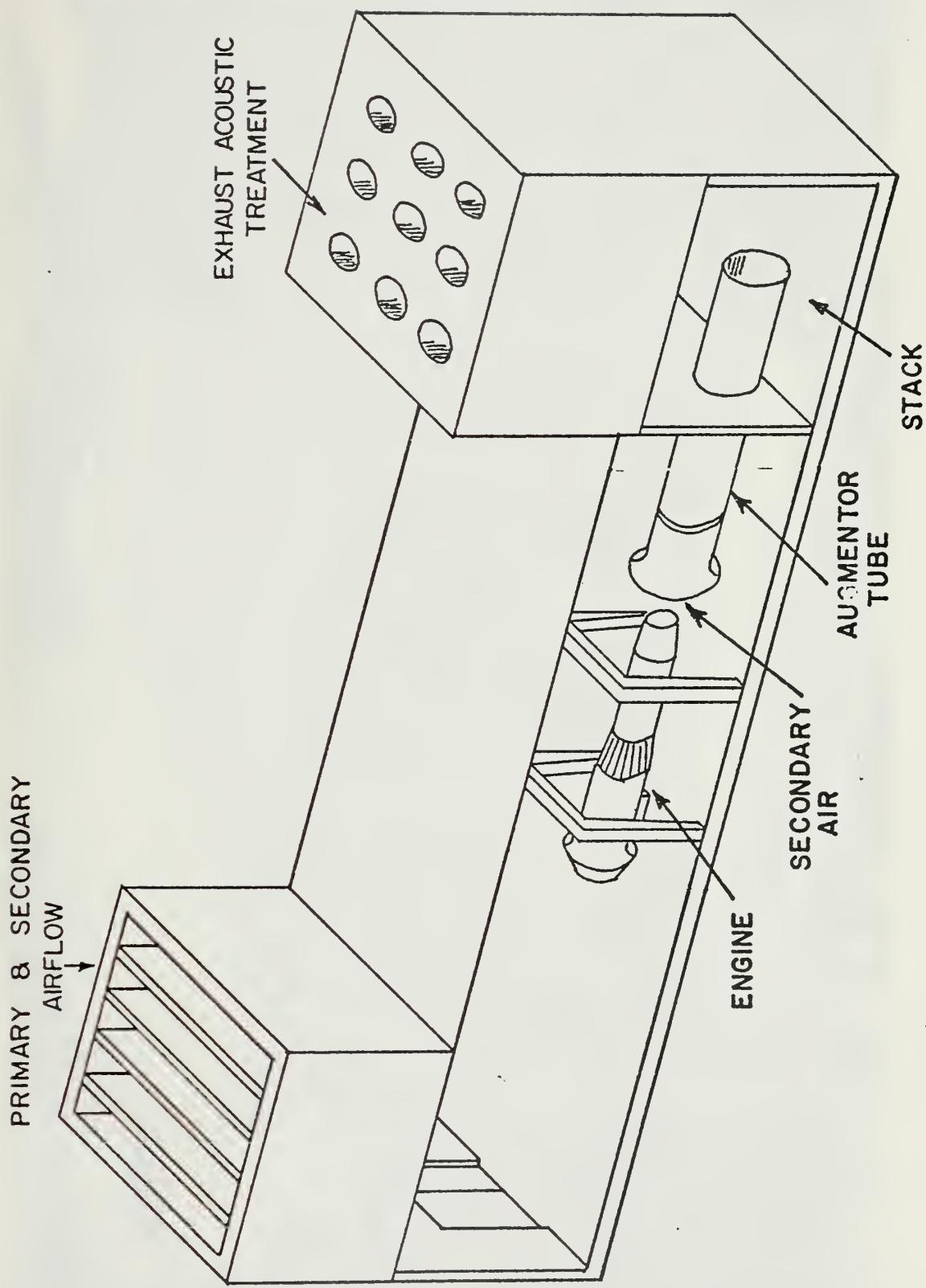
Fig. 1. Schematic of Typical Turbojet Test Cell (from Ref. 9).

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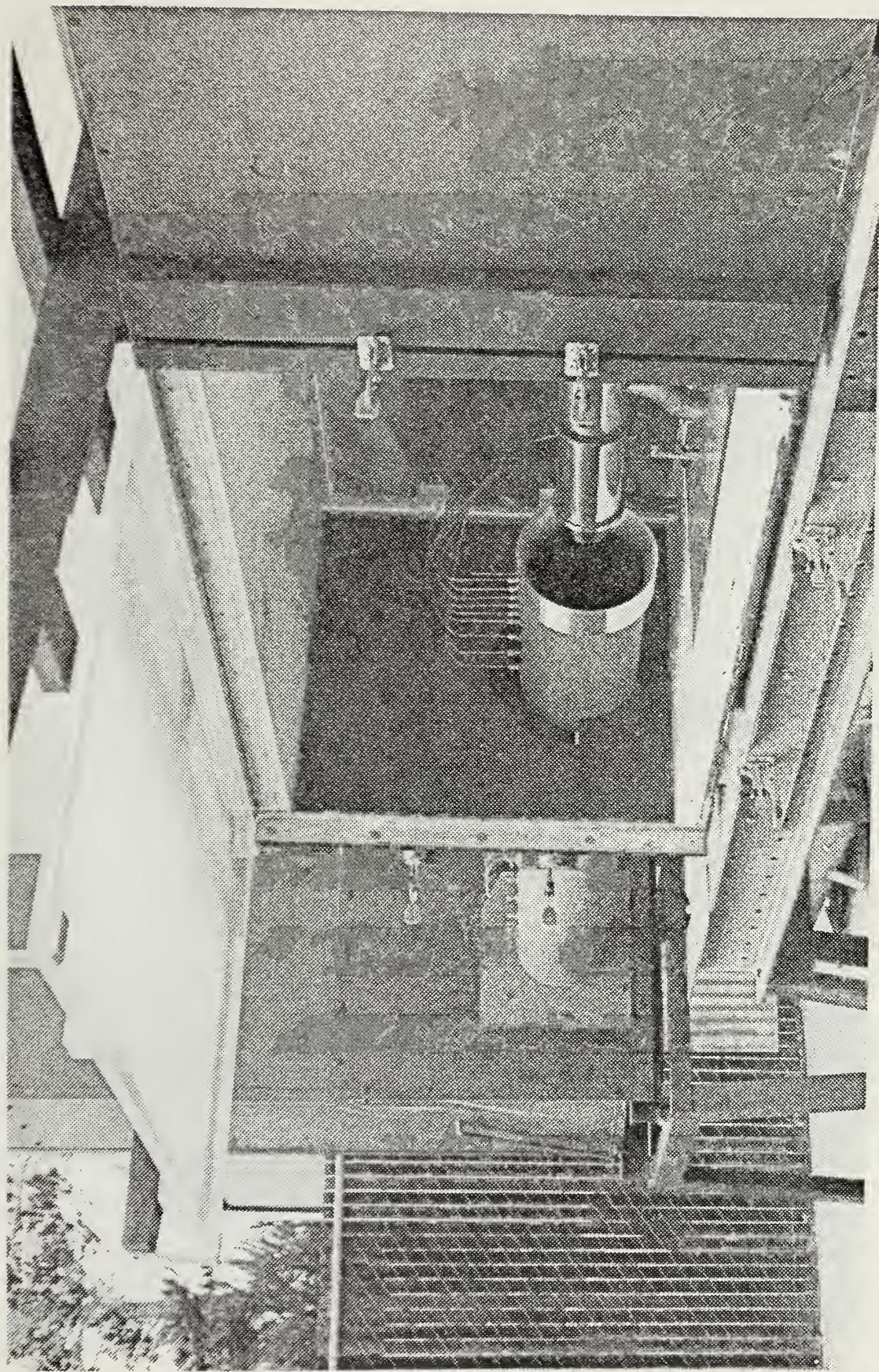
Fig. 2. Cell Test Section
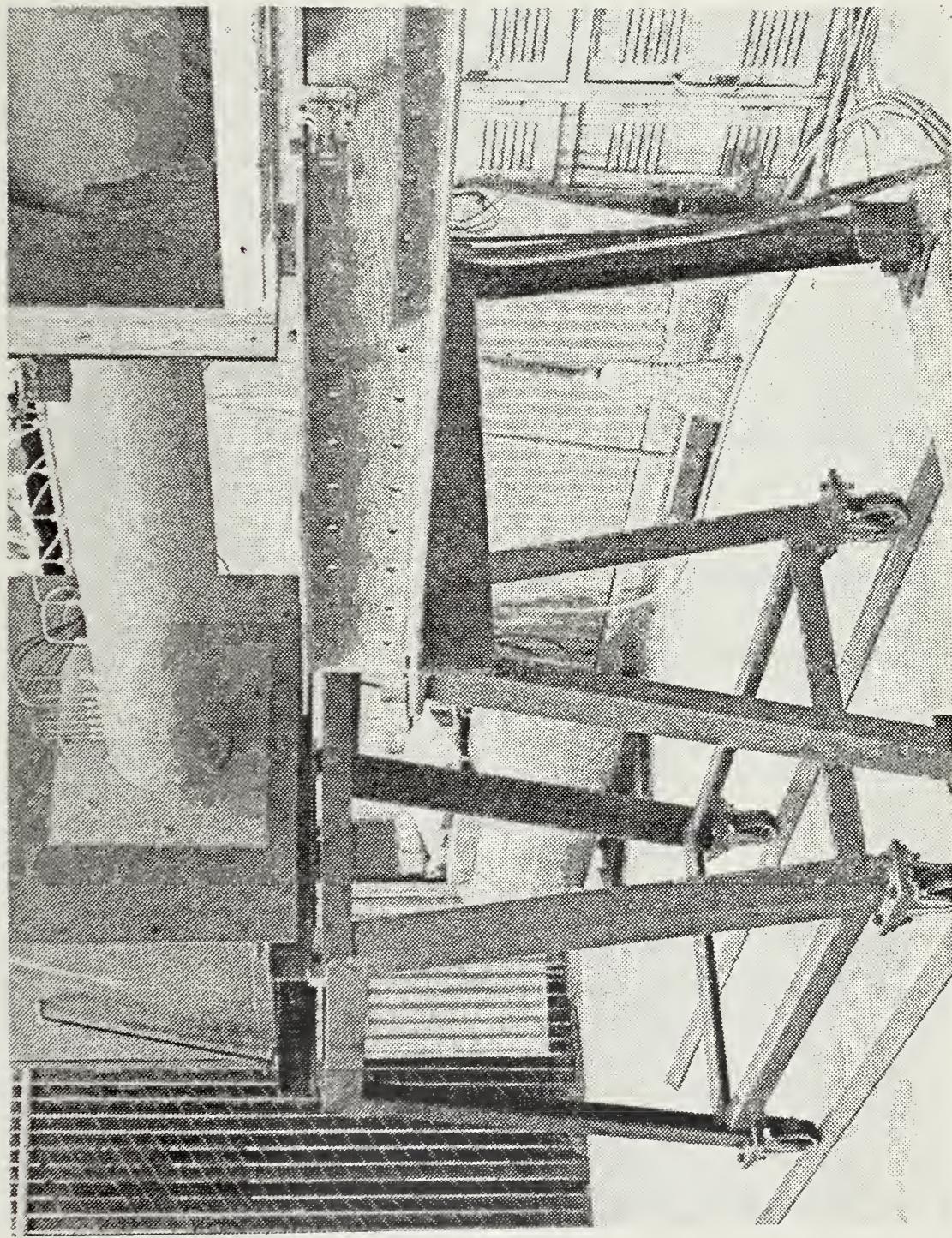
Fig. 3. Augmentor Tube and Stack Translation Apparatus
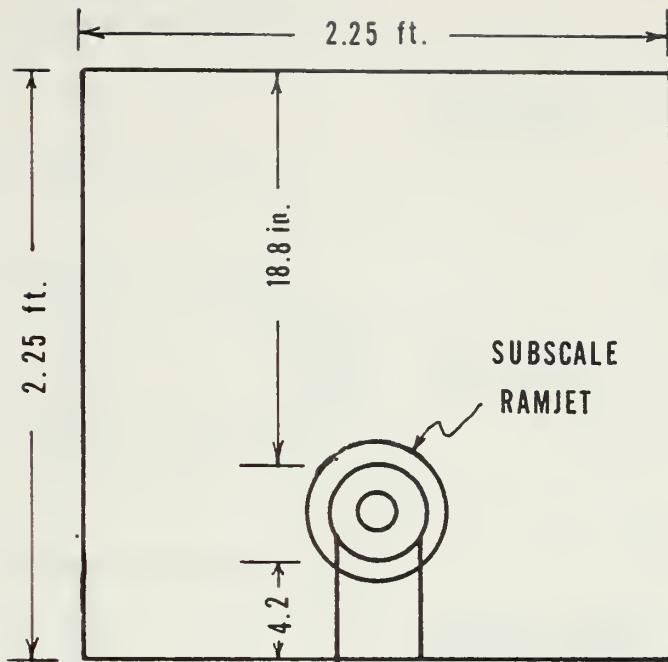
Fig. 4. End View of Test Section (Facing Upstream).
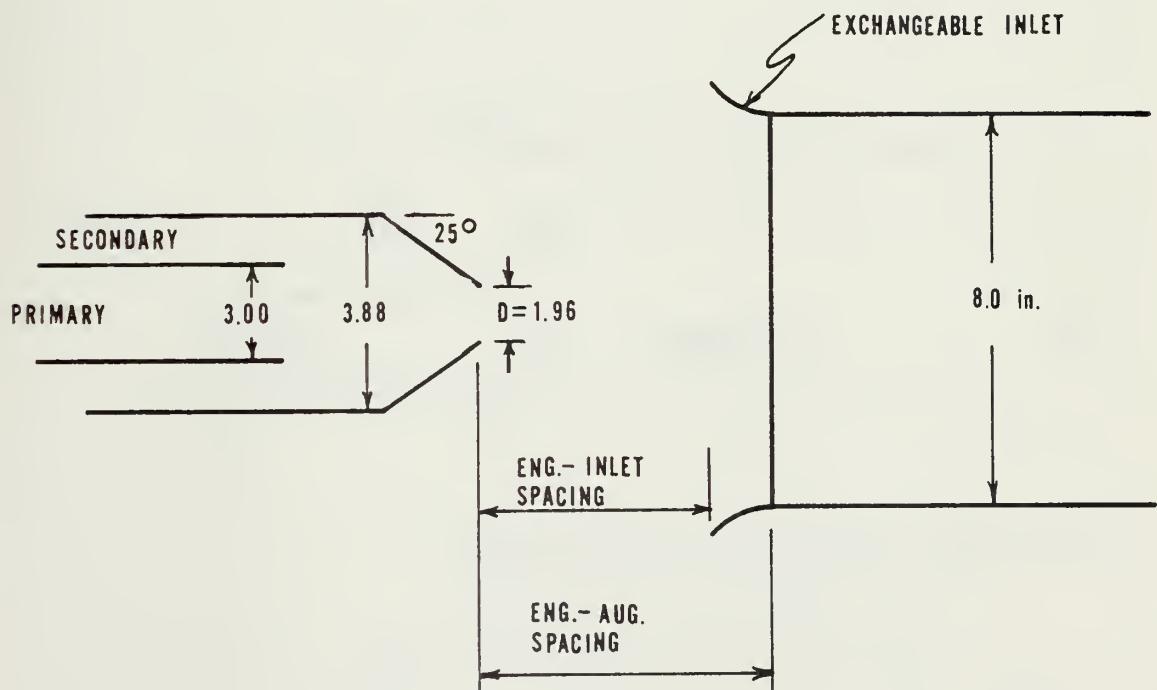
Fig. 5. Engine-Augmentor Interface Geometry.
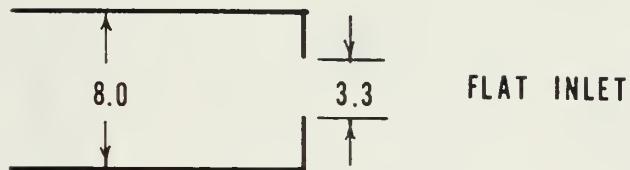
Fig. 6. Augmentor Inlet Variations.
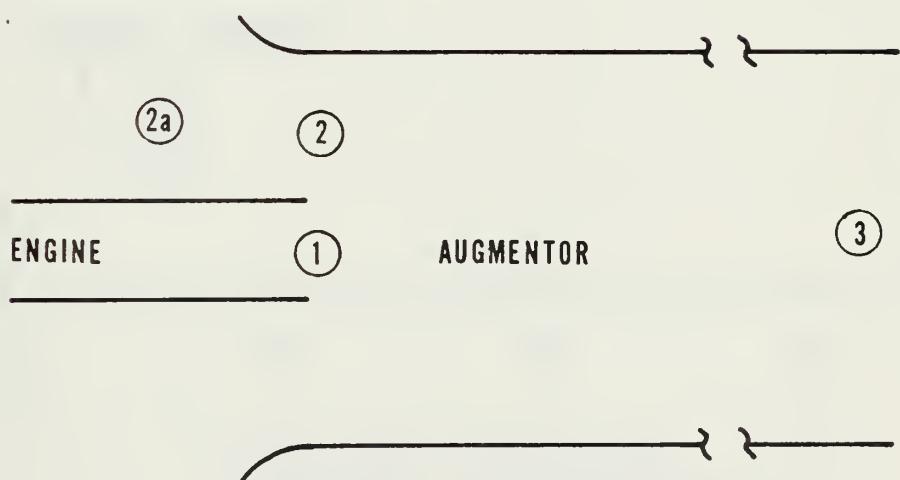
Fig. 7. One-Dimensional Model Geometry.
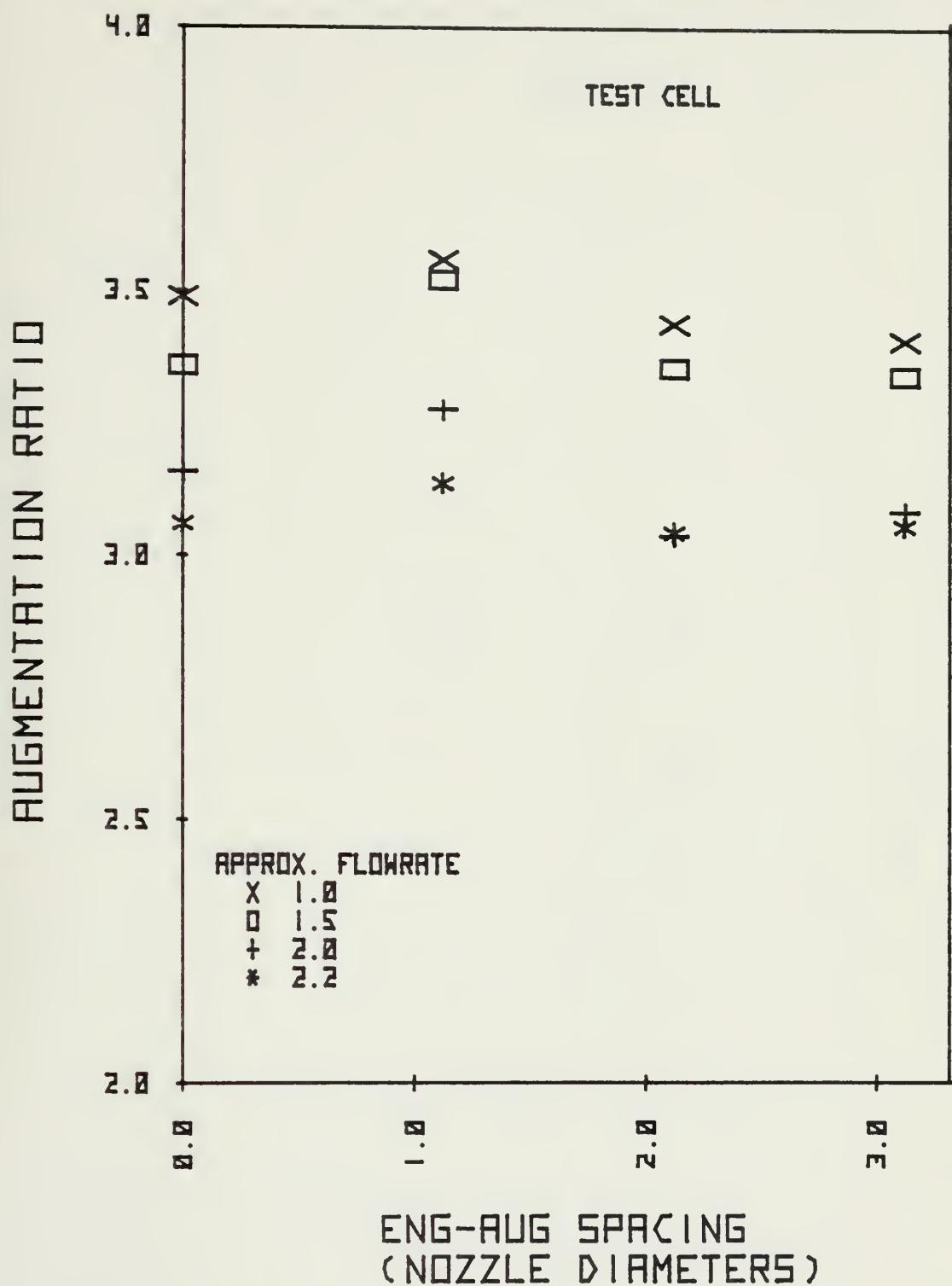
Fig. 8. Augmentation Ratio vs. Spacing (Test Cell).

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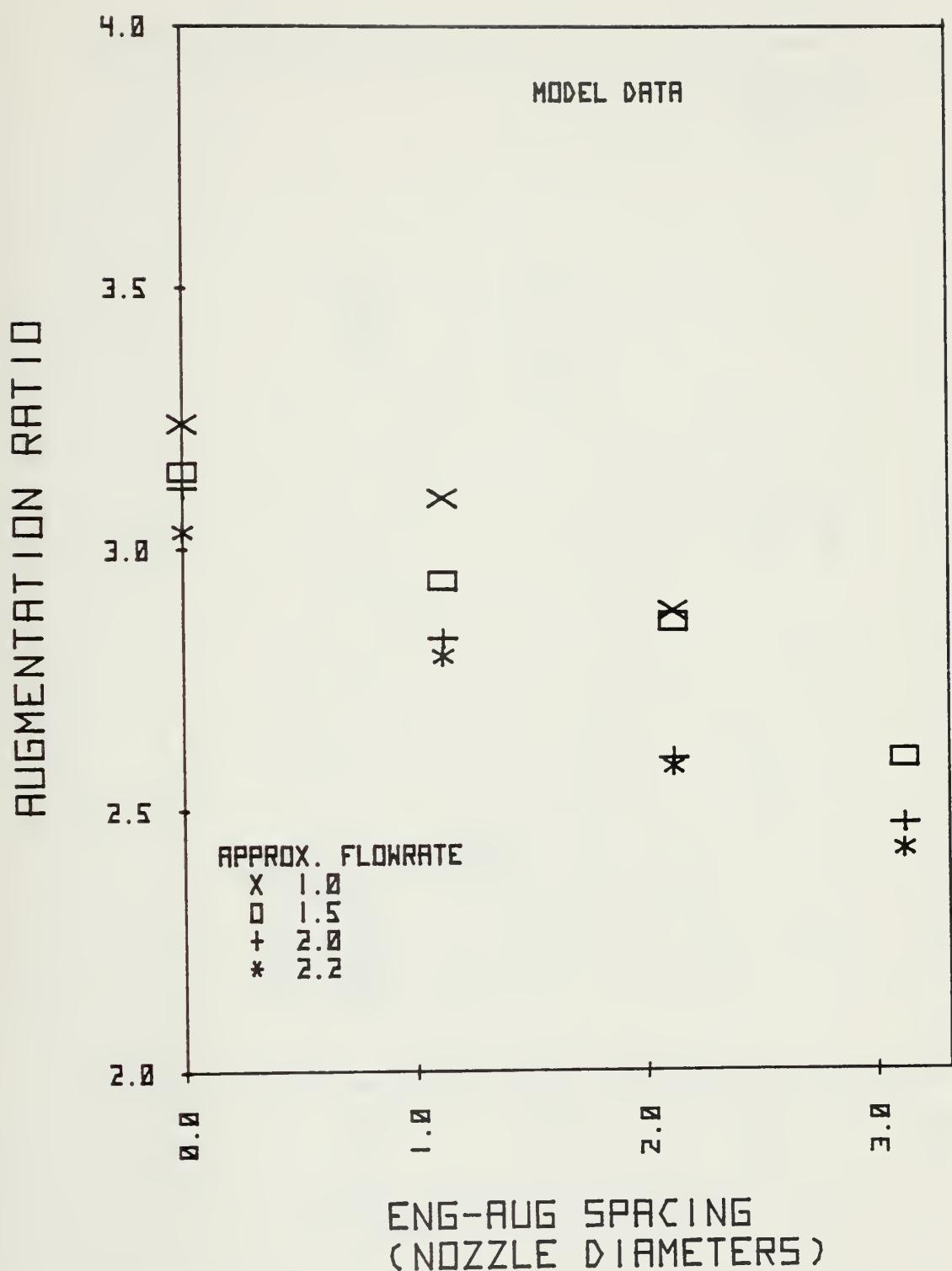
Fig. 9.   Augmentation Ratio vs. Spacing (Model).
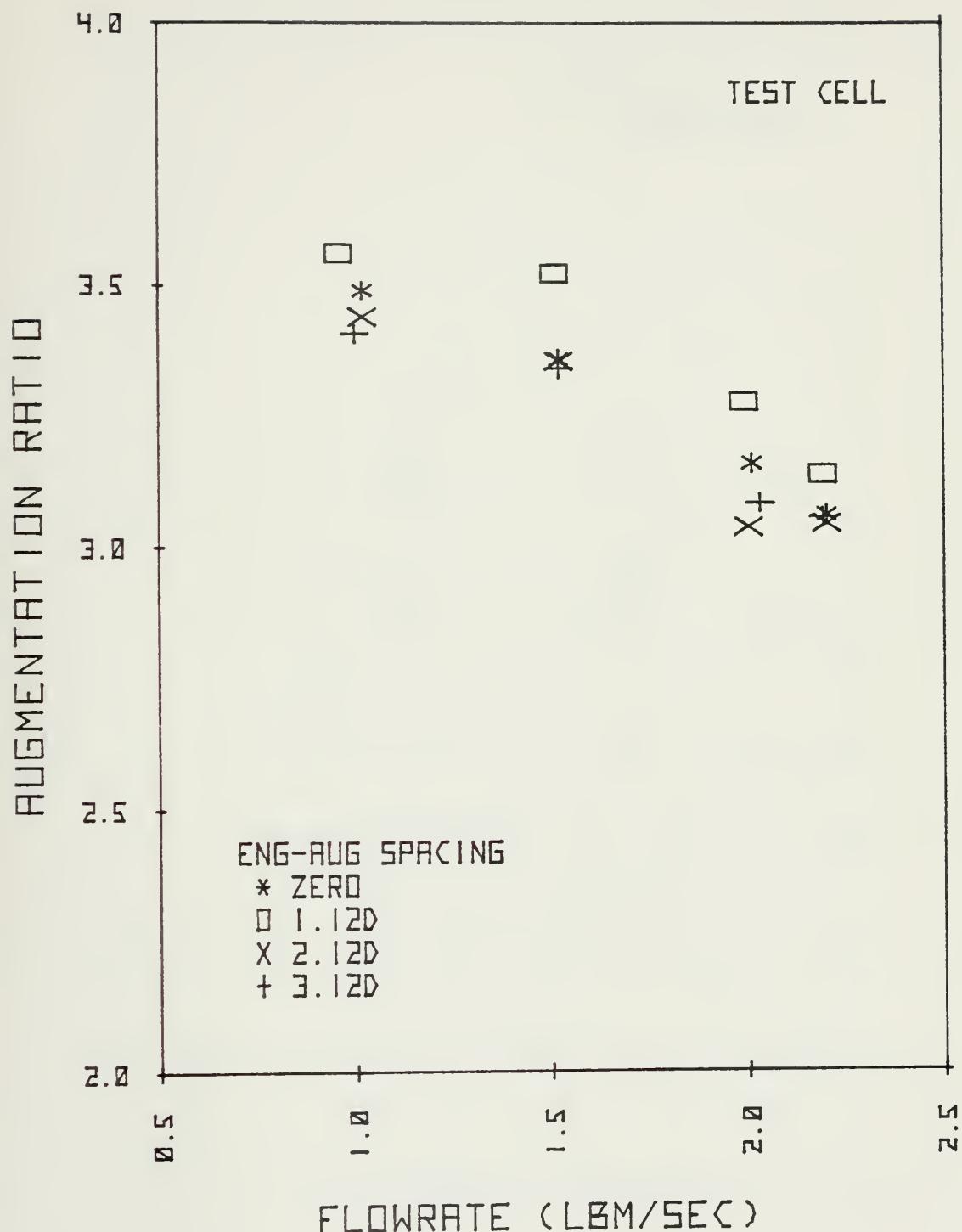
Fig. 10. Augmentation Ratio vs. Flow Rate (Test Cell).
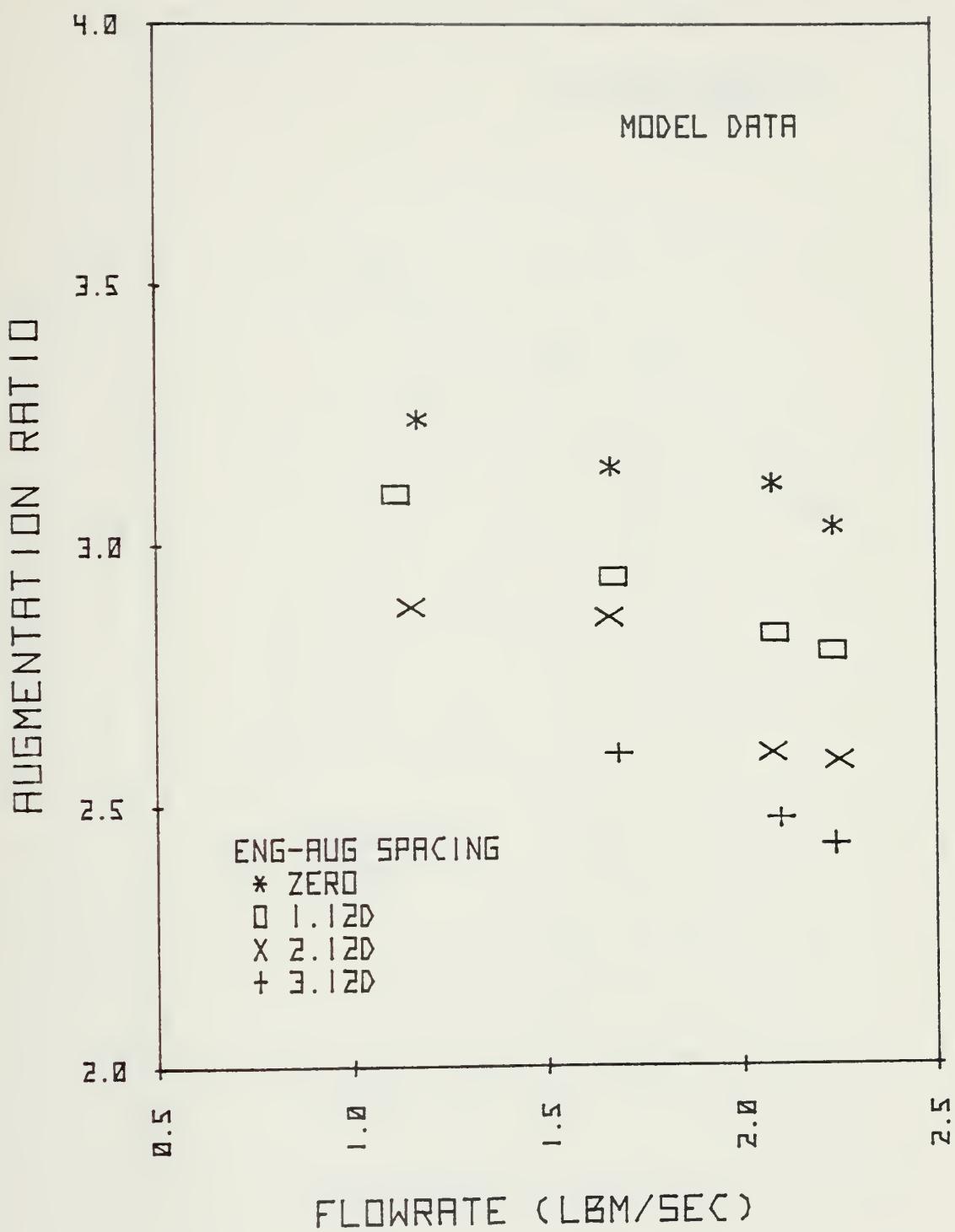
Fig. 11. Augmentation Ratio vs. Flow Rate (Model).
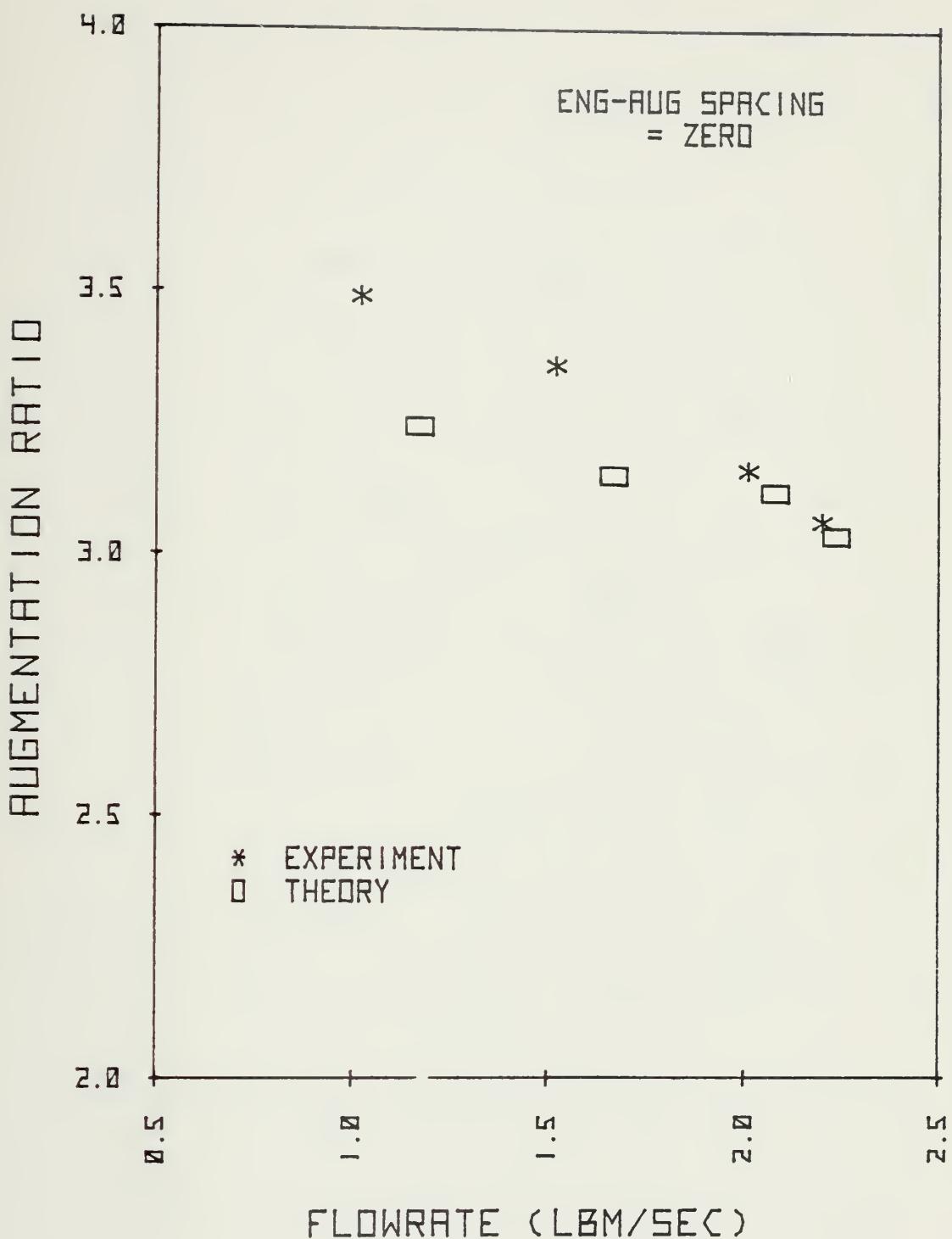
Fig. 12.   Augmentation Ratio vs. Flow Rate.
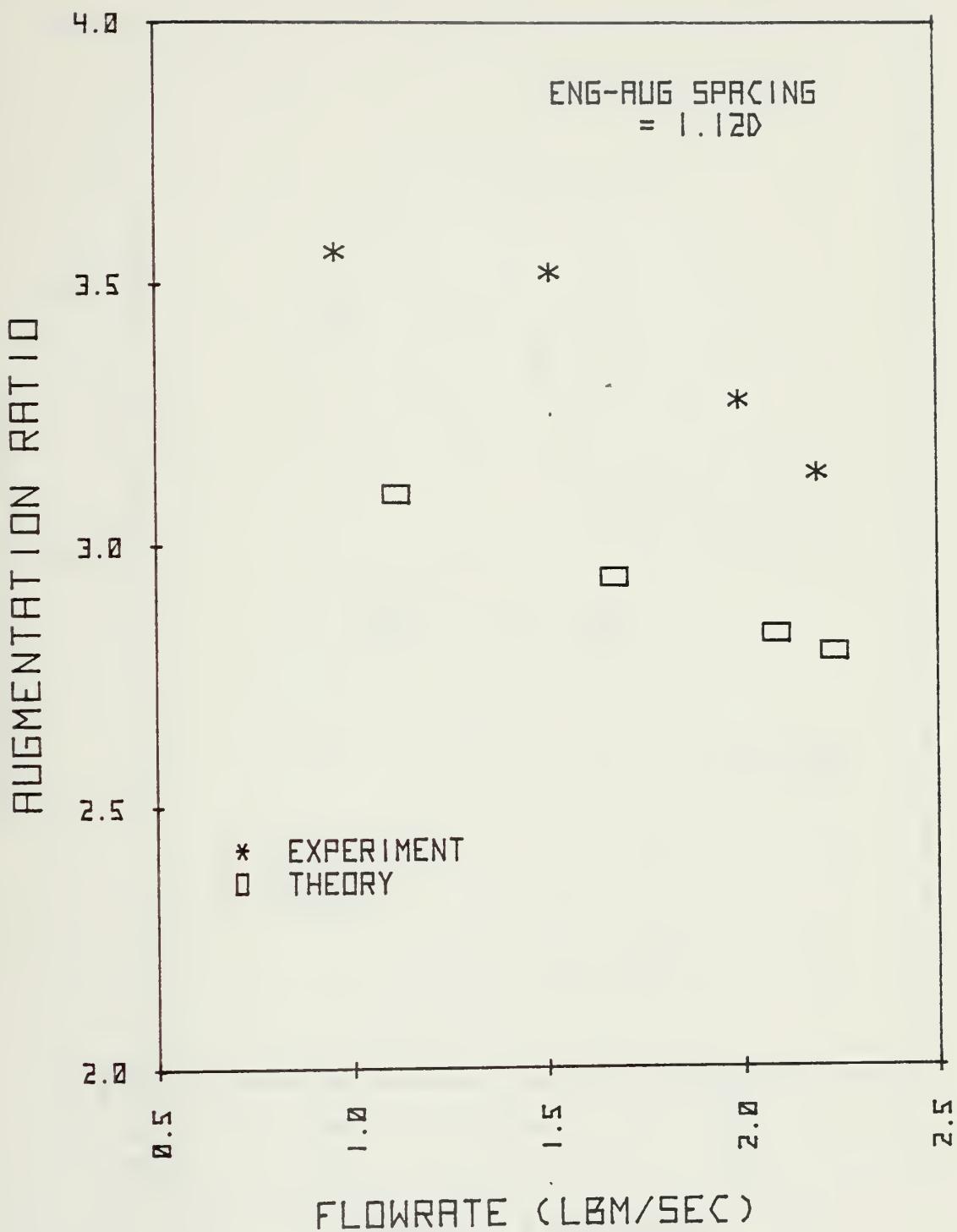
Fig. 13.   Augmentation Ratio vs. Flow Rate.
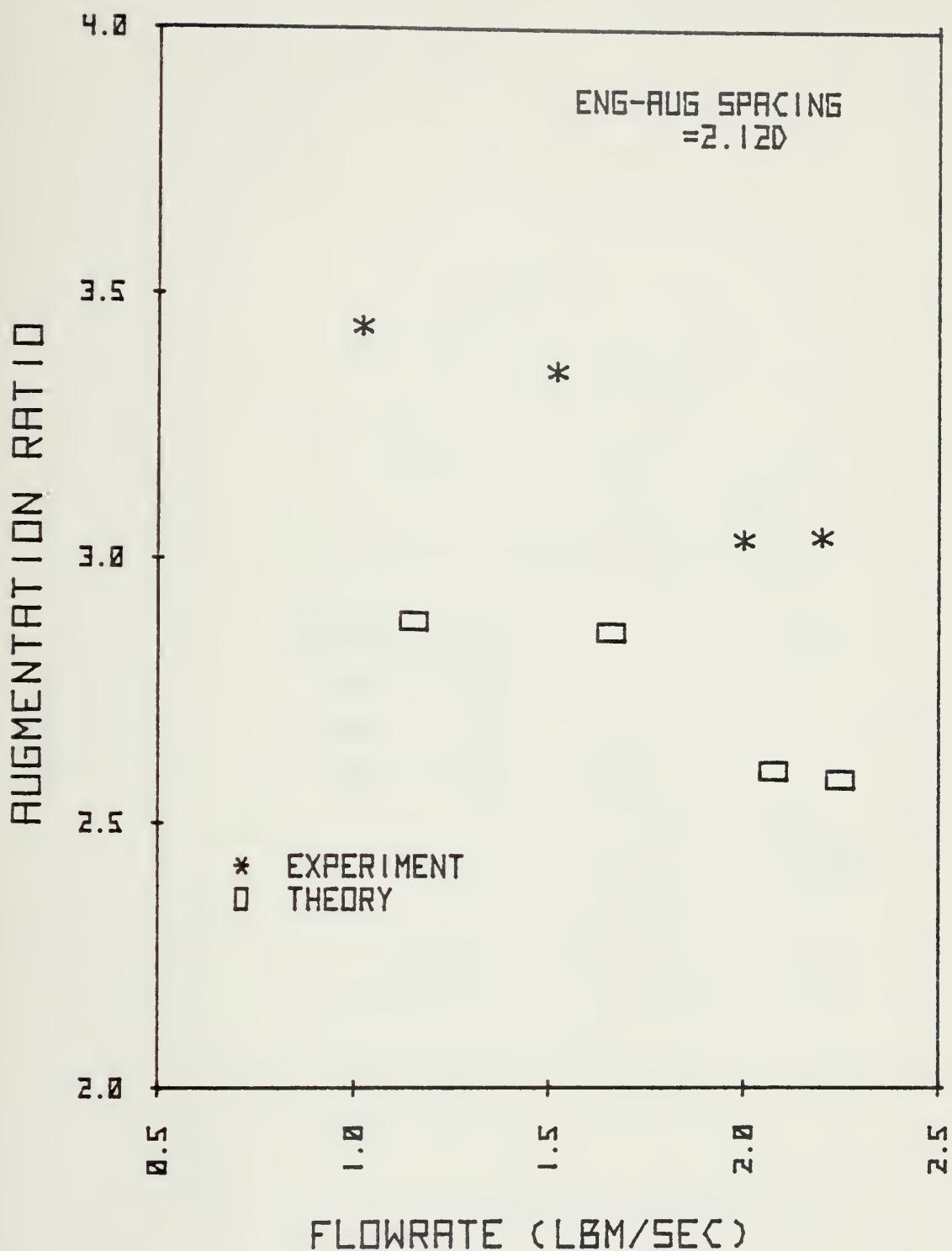
Fig. 14. Augmentation Ratio vs. Flow Rate.
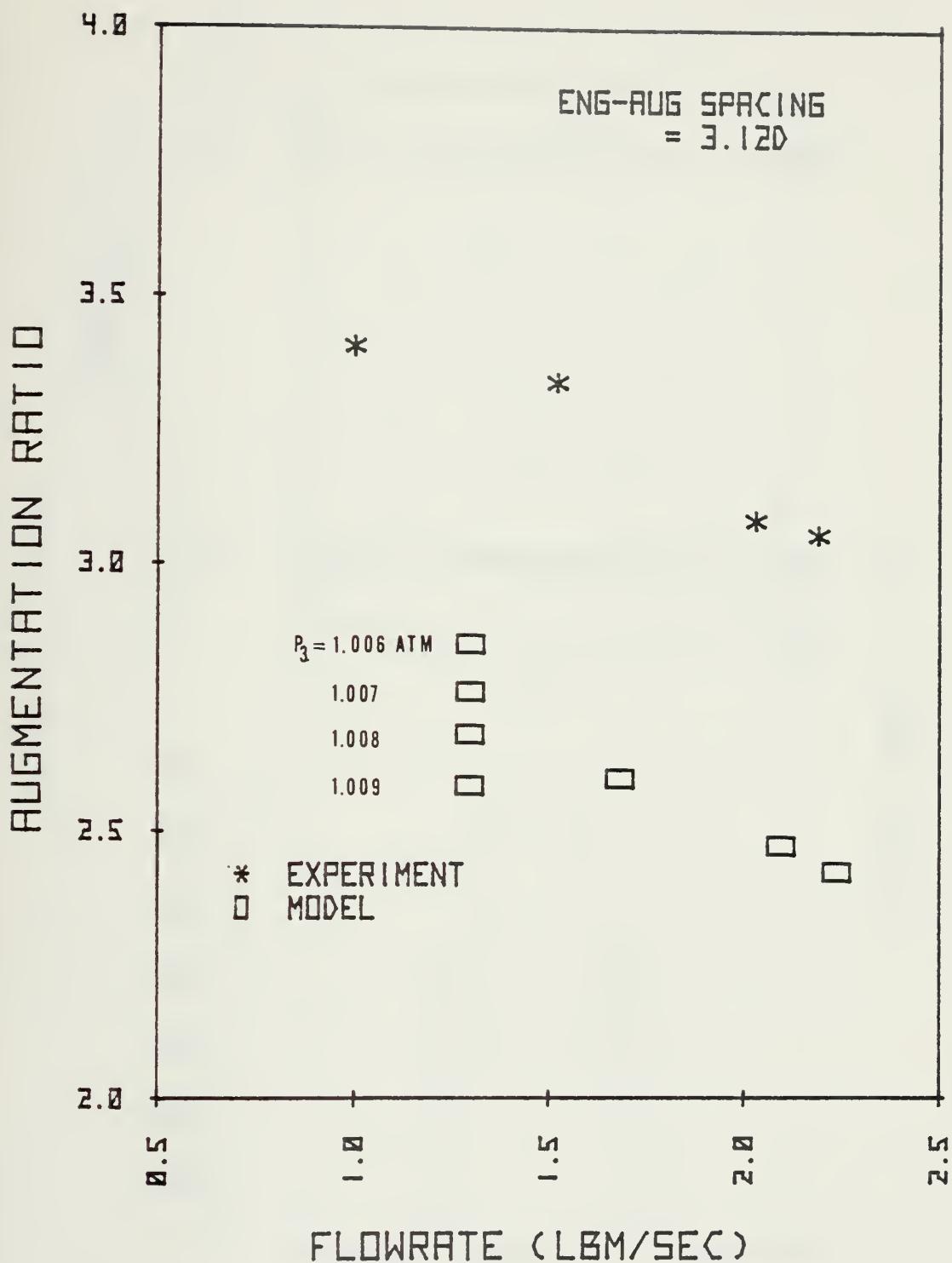
Fig. 15. Augmentation Ratio vs. Flow Rate.

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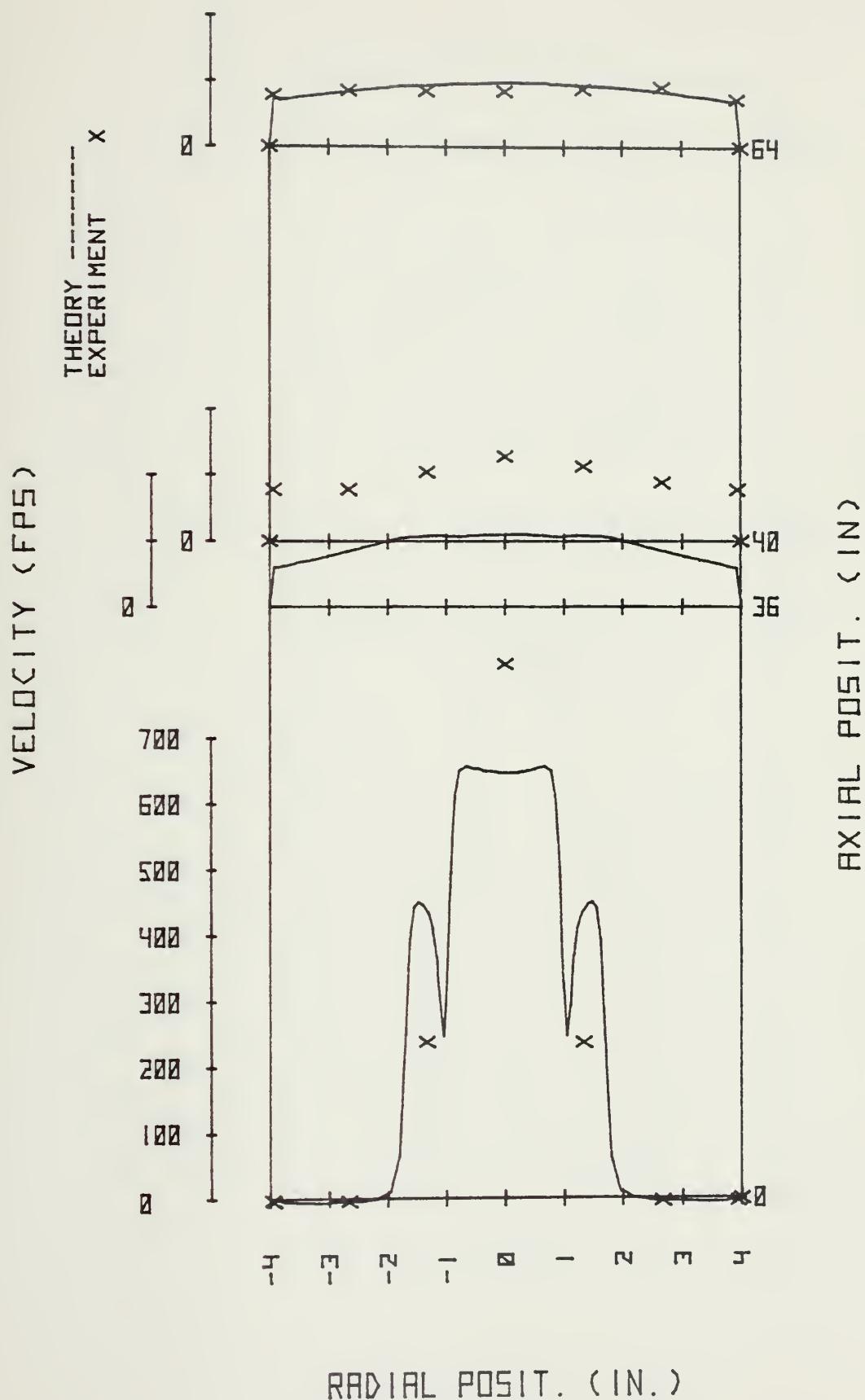
Fig. 16. Augmentor Velocity Profiles: Flow Rate = 1.0; Spacing = 0;
Augmentation Ratio = 1.26; Cold Flow.

VELOCITY (FPS)

THEORY ------
EXPERIMENT   X
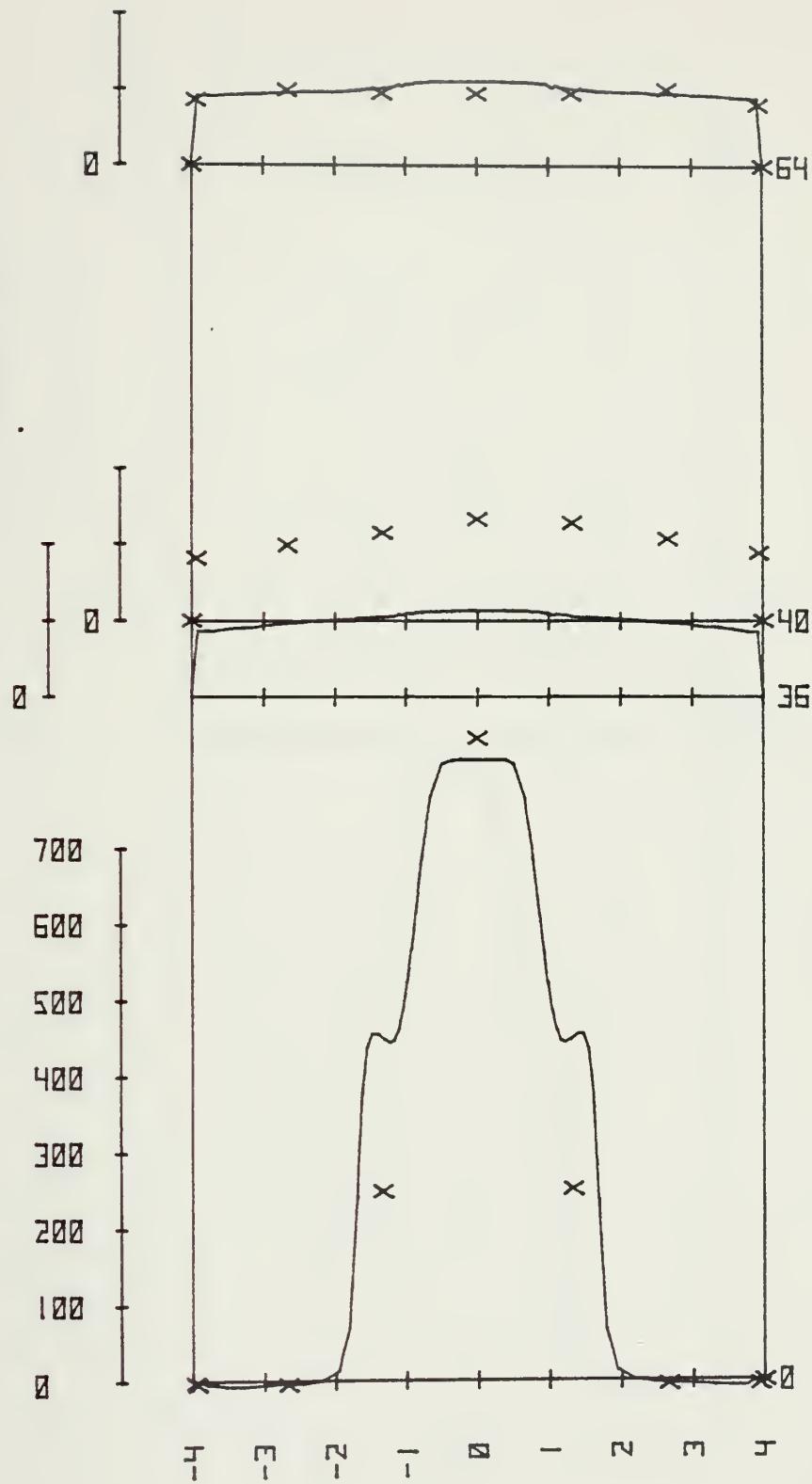
Fig. 17.   Augmentor Velocity Profiles: Flow Rate = 1.0; Spacing = 1
Nozzle Diameter; Augmentation Ratio = 1.52; Cold Flow.

AXIAL POSIT. (IN)

RADIAL POSIT. (IN.)
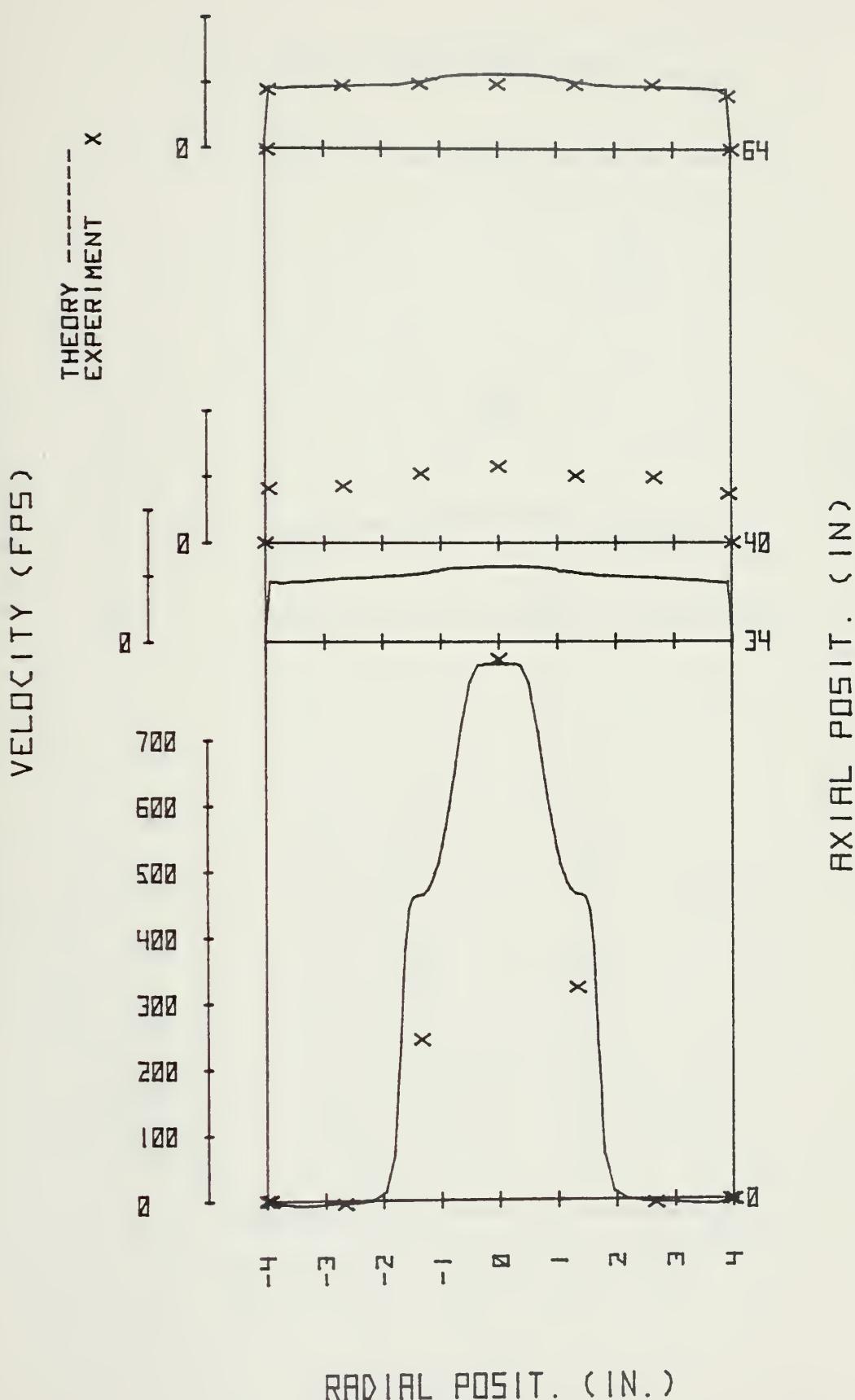
Fig. 18. Augmentor Velocity Profiles; Flow Rate = .99; Spacing = 2 Nozzle Diameters; Augmentation Ratio = 1.55; Cold Flow.
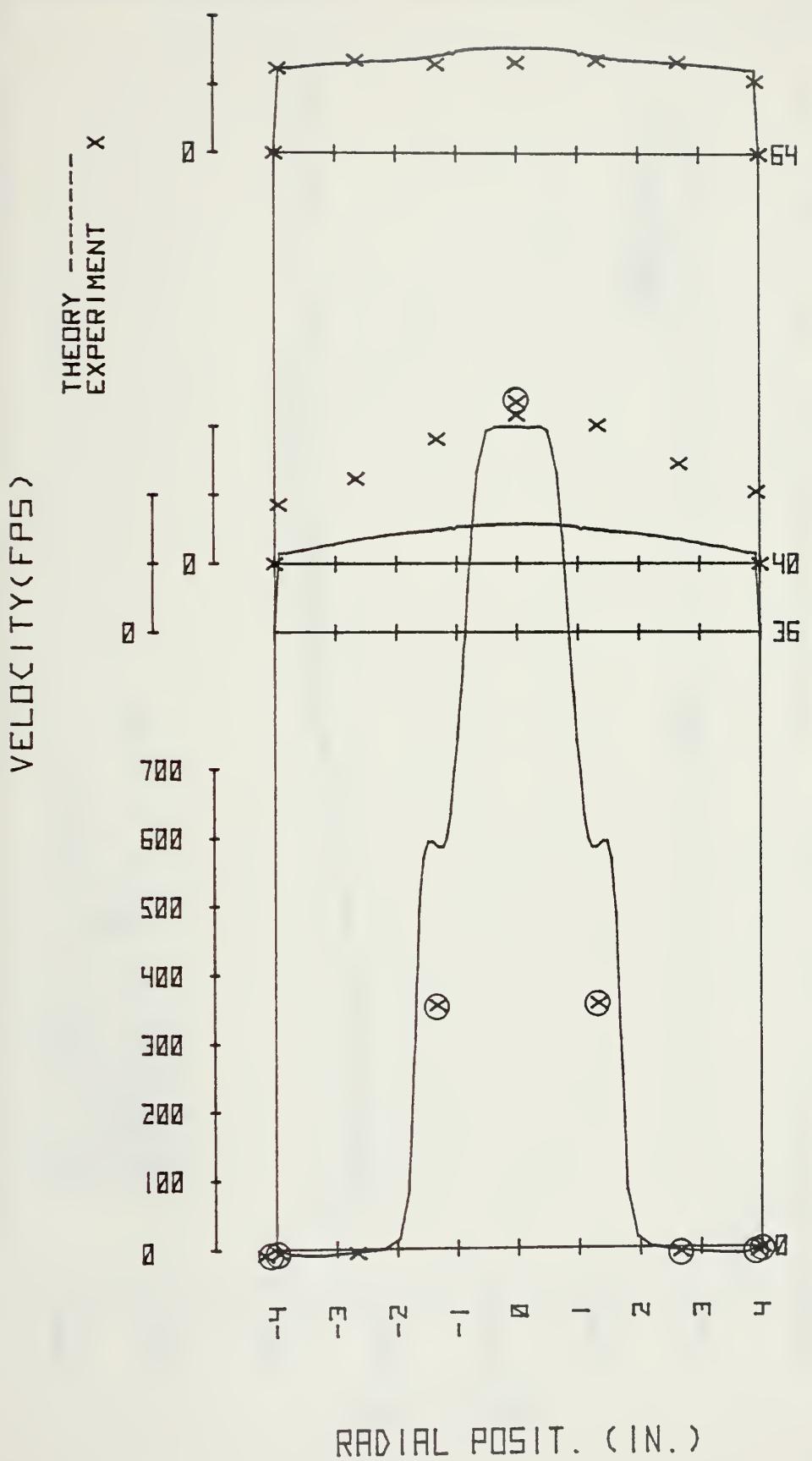
Fig. 19. Augmentor Velocity Profiles: Flow Rate = 1.52, Spacing = 1 Nozzle Diameter; Augmentation Ratio = 1.29; Cold Flow.
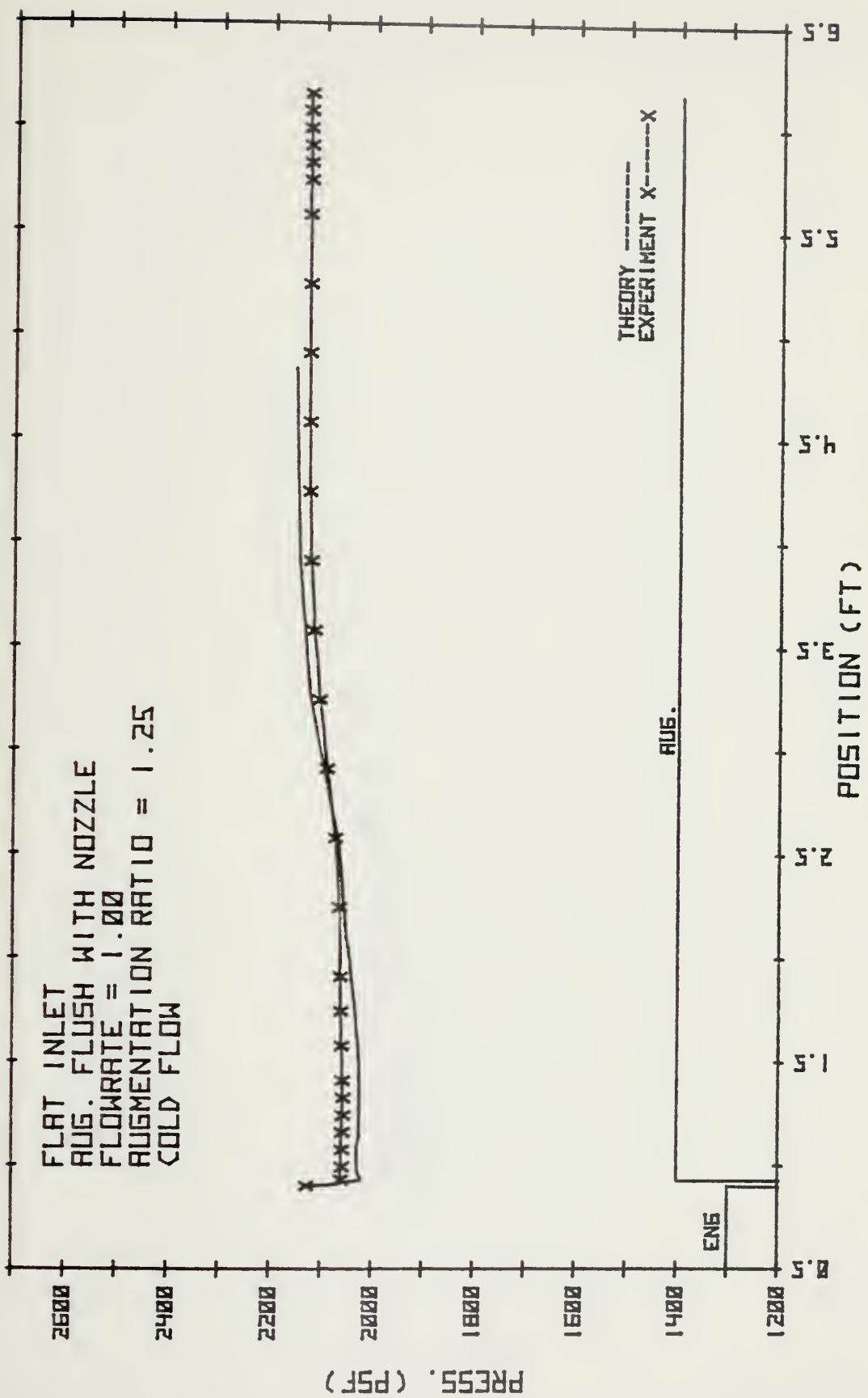
Fig. 20. Augmentor Pressure Profile.

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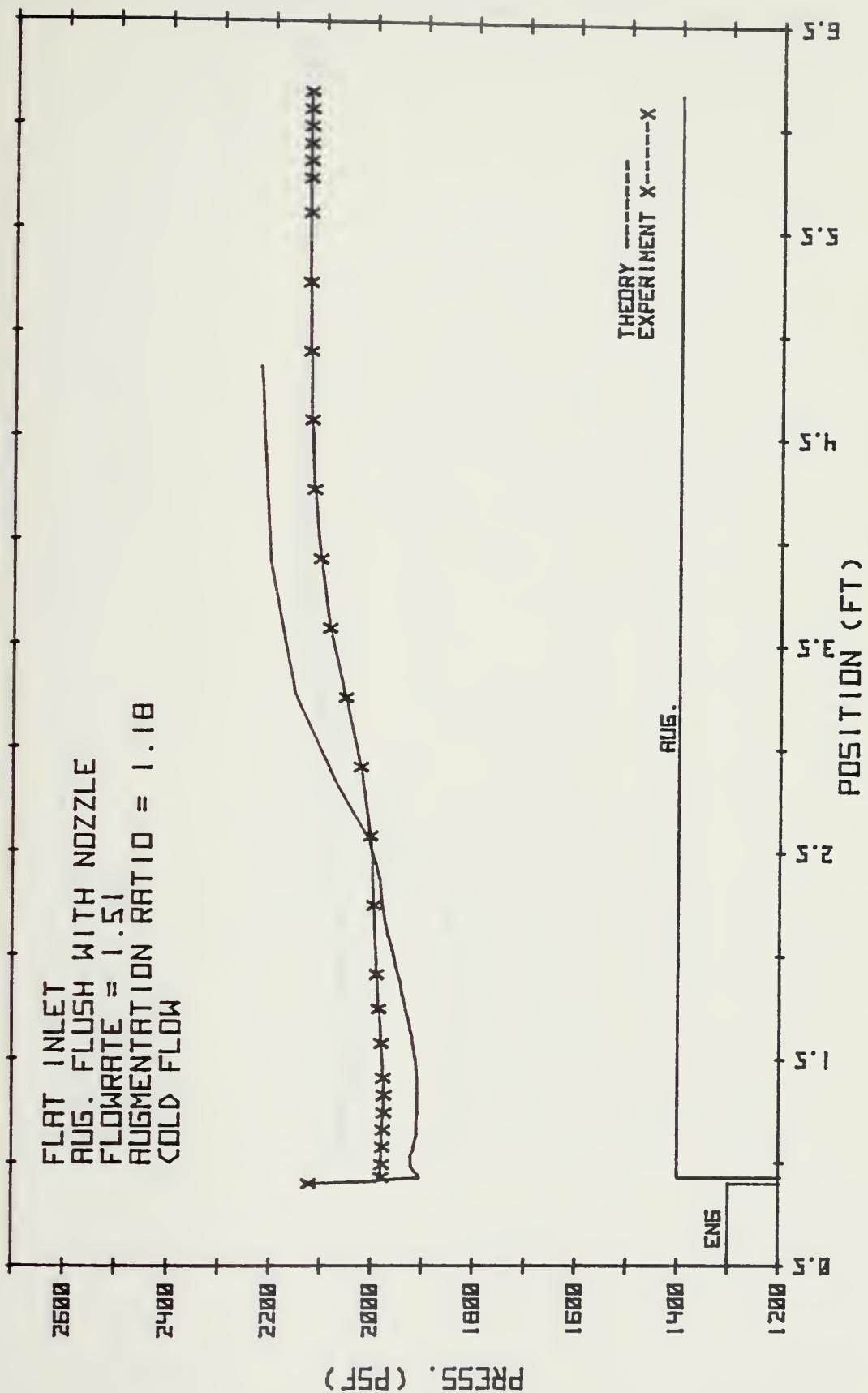
Fig. 21. Augmentor Pressure Profile.
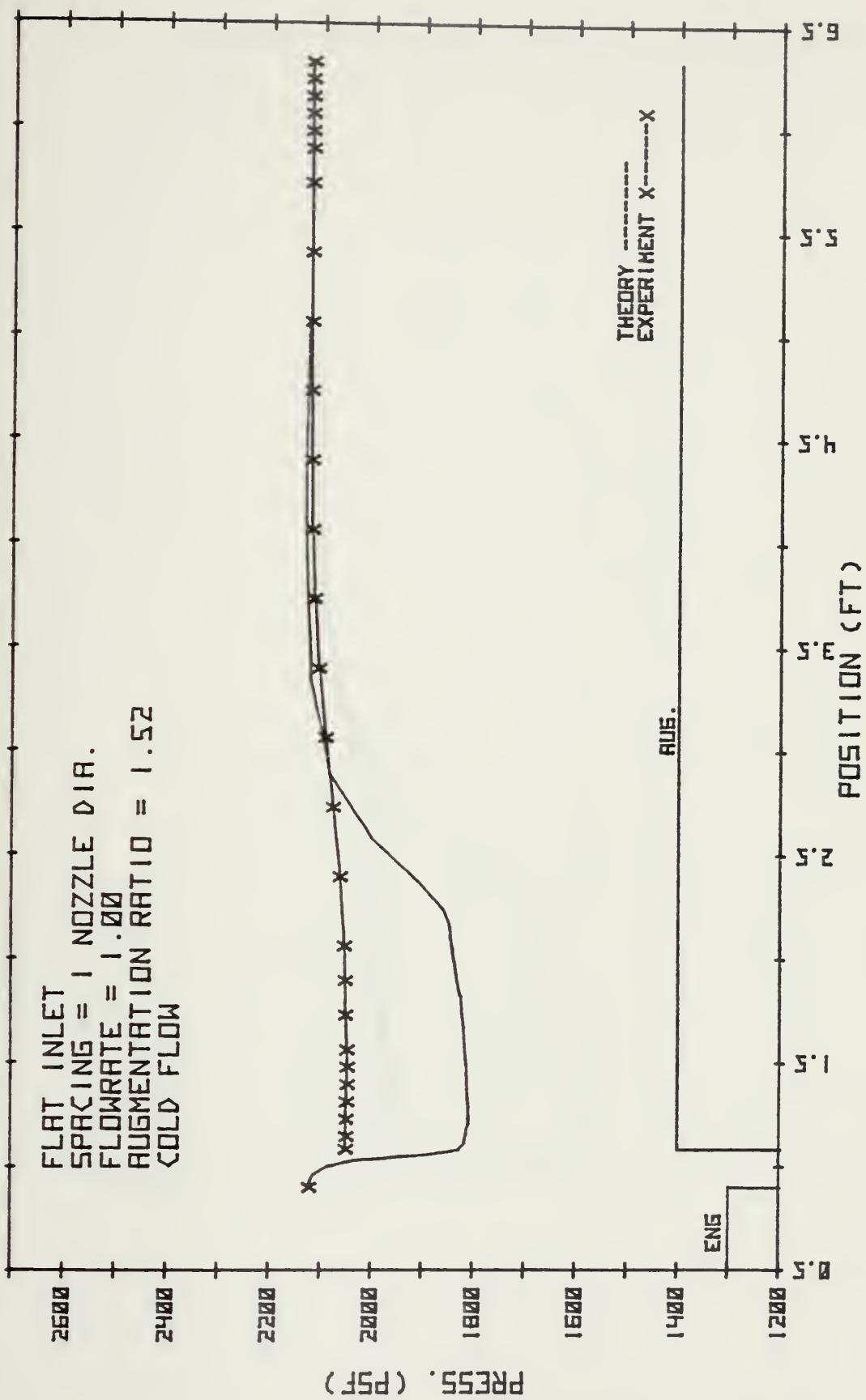
Fig. 22. Augmentor Pressure Profile.
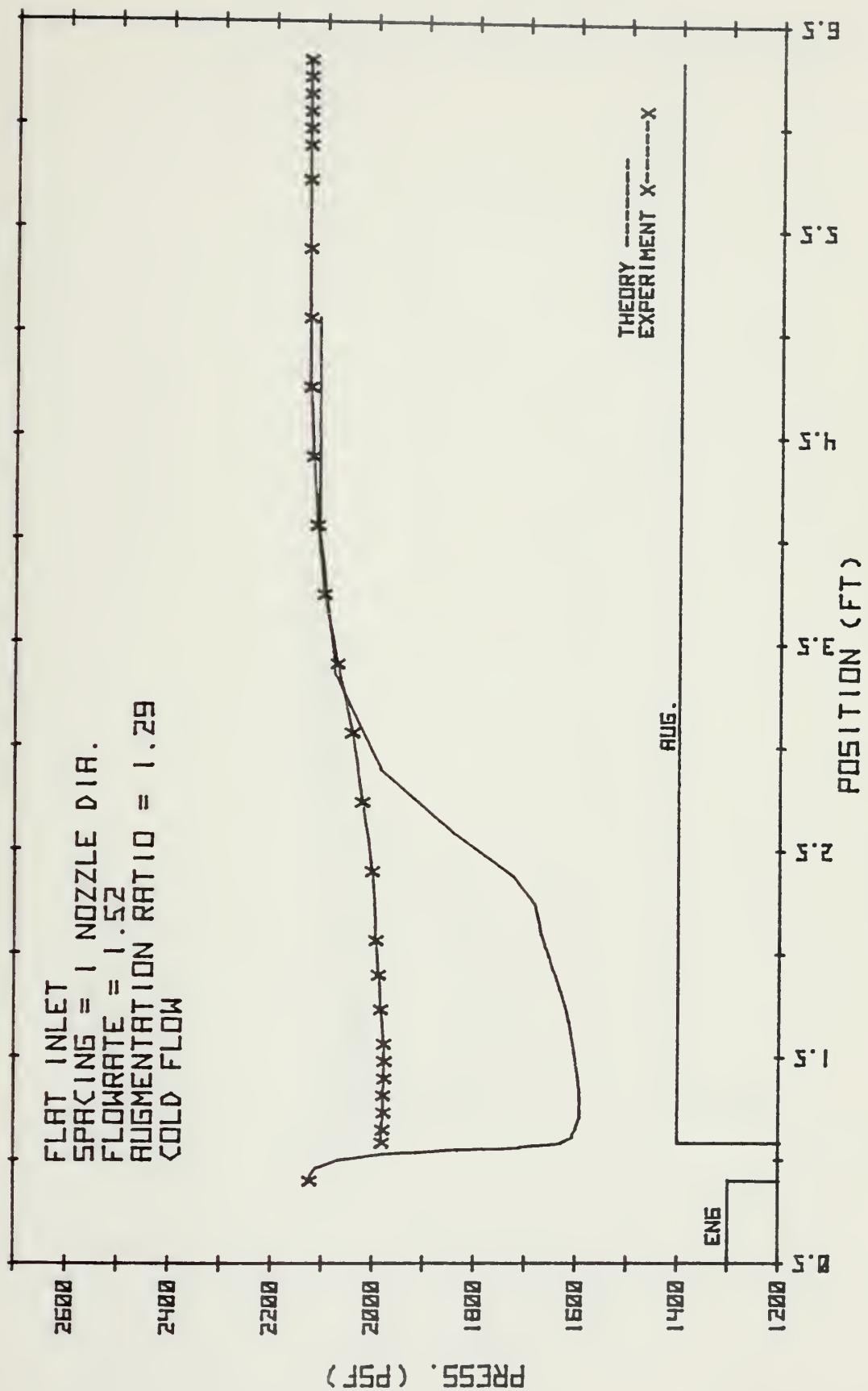
Fig. 23. Augmentor Pressure Profile.

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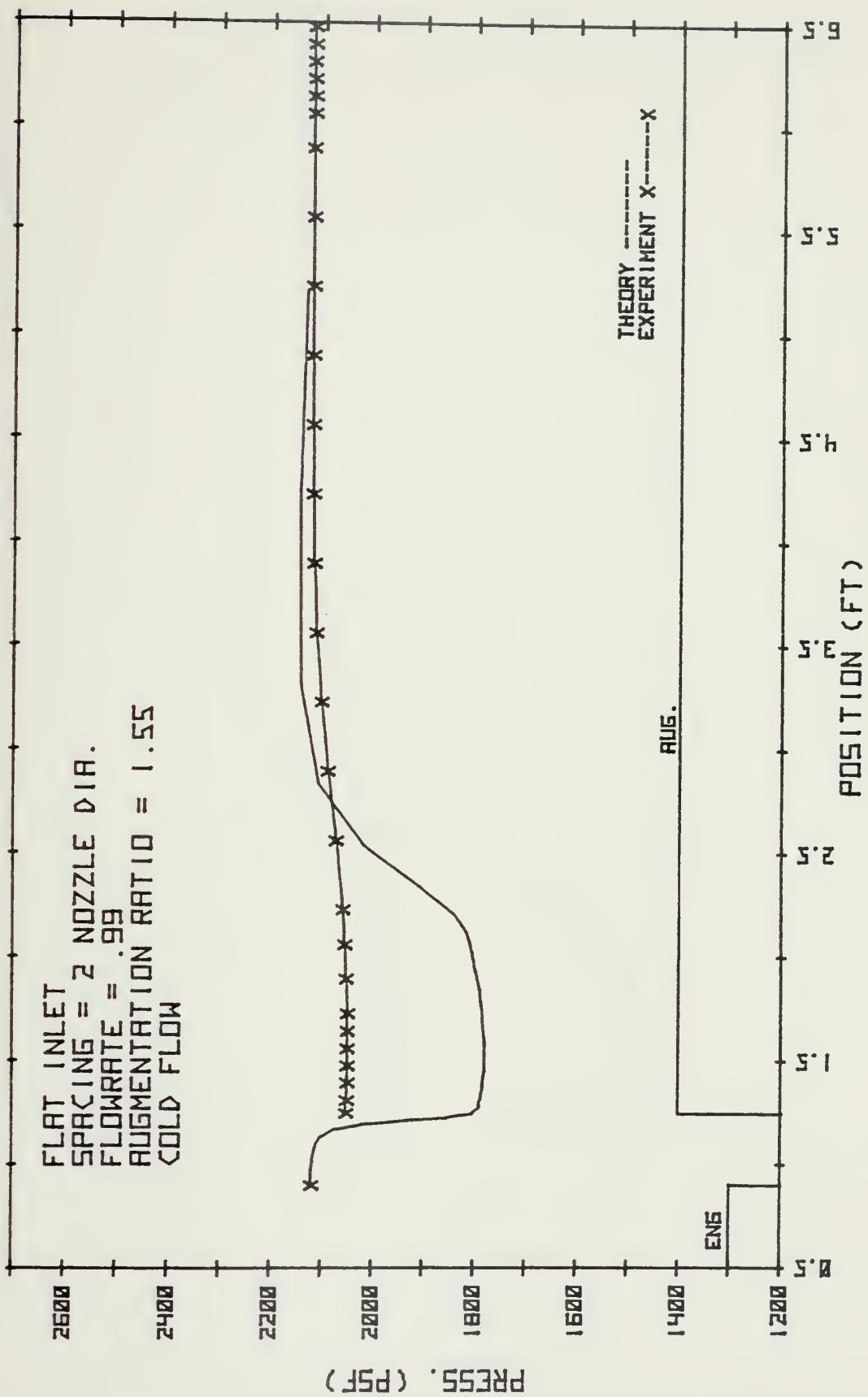
Fig. 24. Augmentor Pressure Profile.
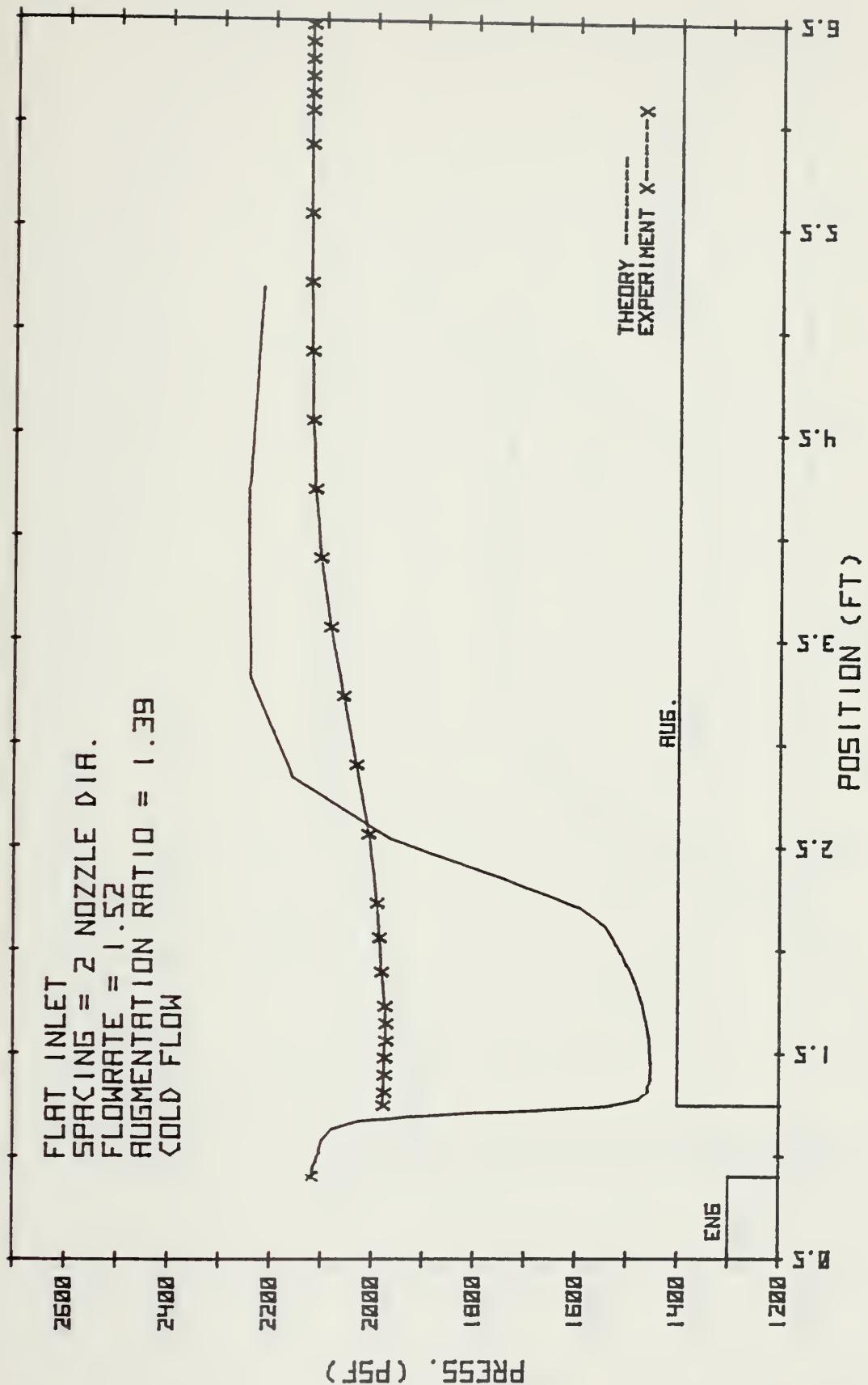
Fig. 25. Augmentor Pressure Profile.
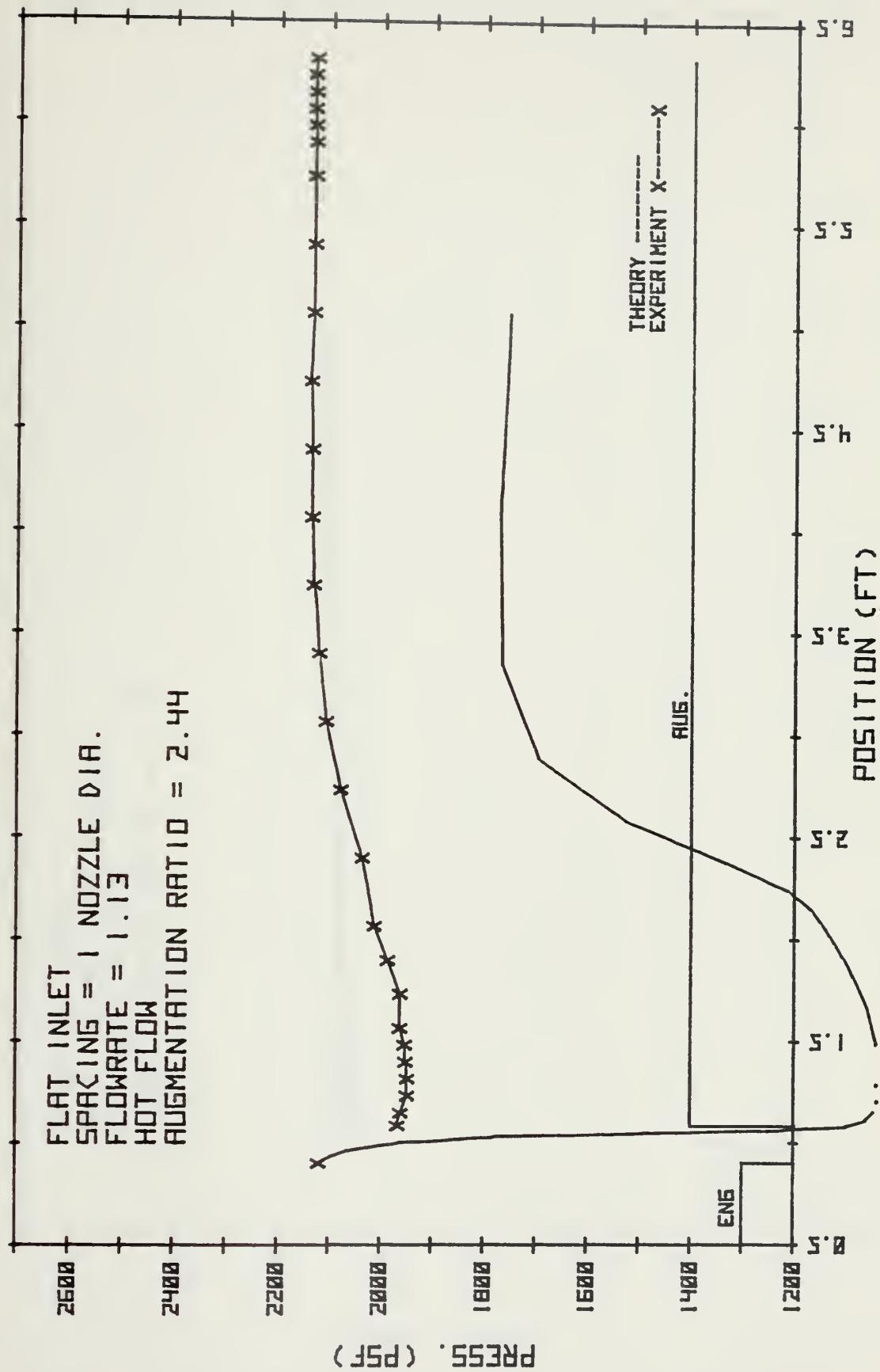
Fig. 26. Augmentor Pressure Profile.
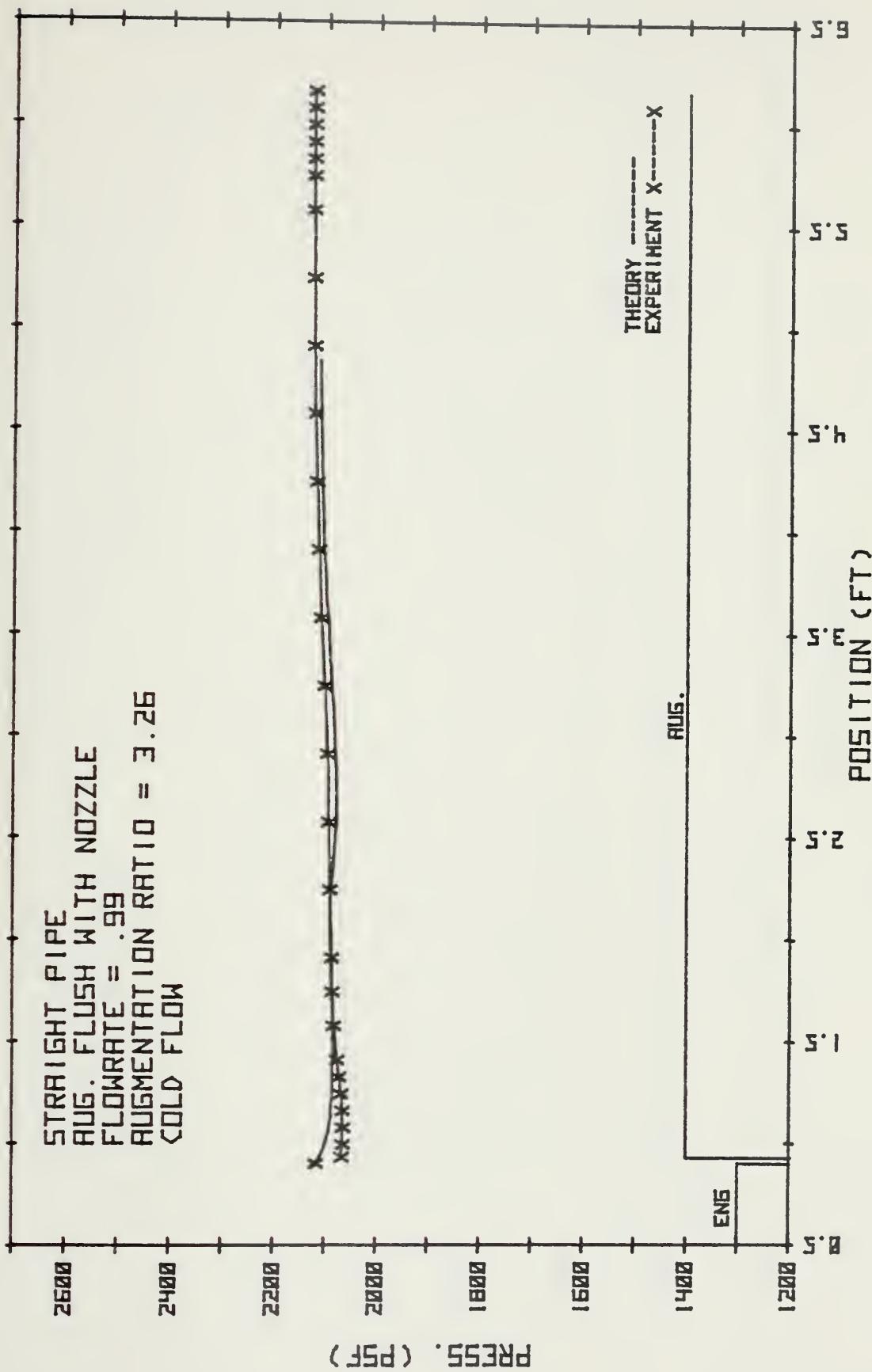
Fig. 27. Augmentor Pressure Profile.
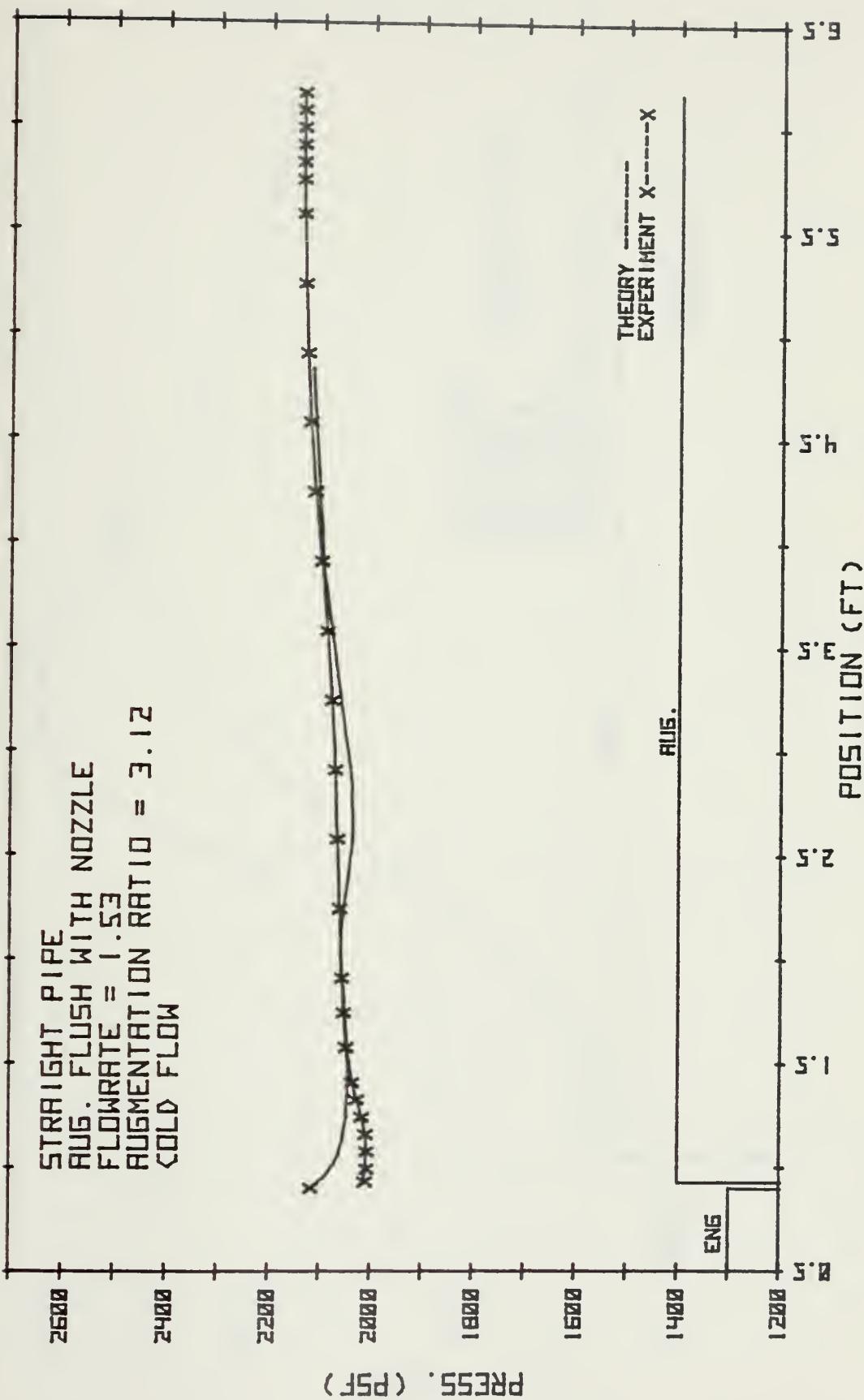
Fig. 28. Augmentor Pressure Profile.

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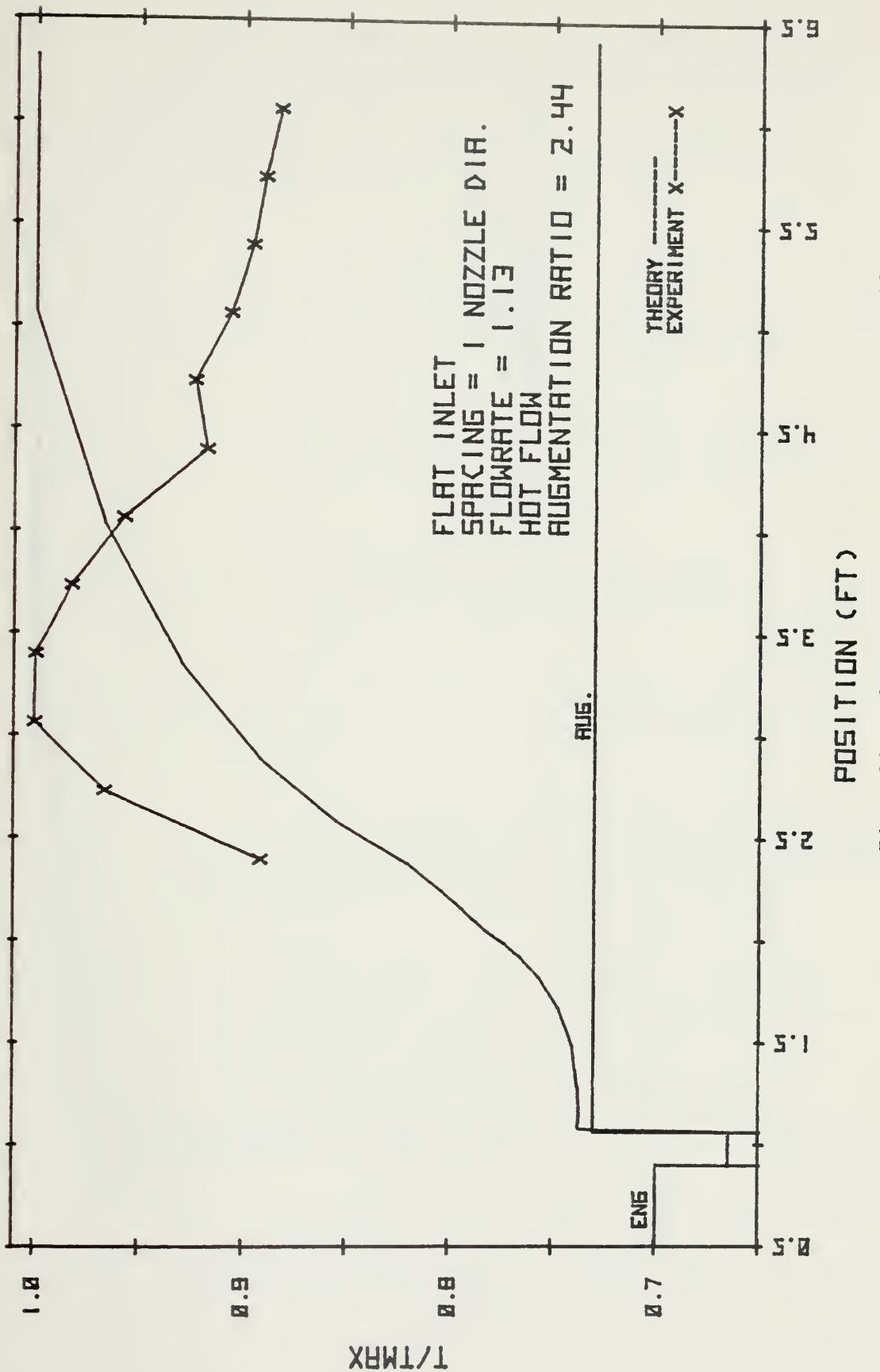
Fig. 29. Augmentor Temperature Profile.
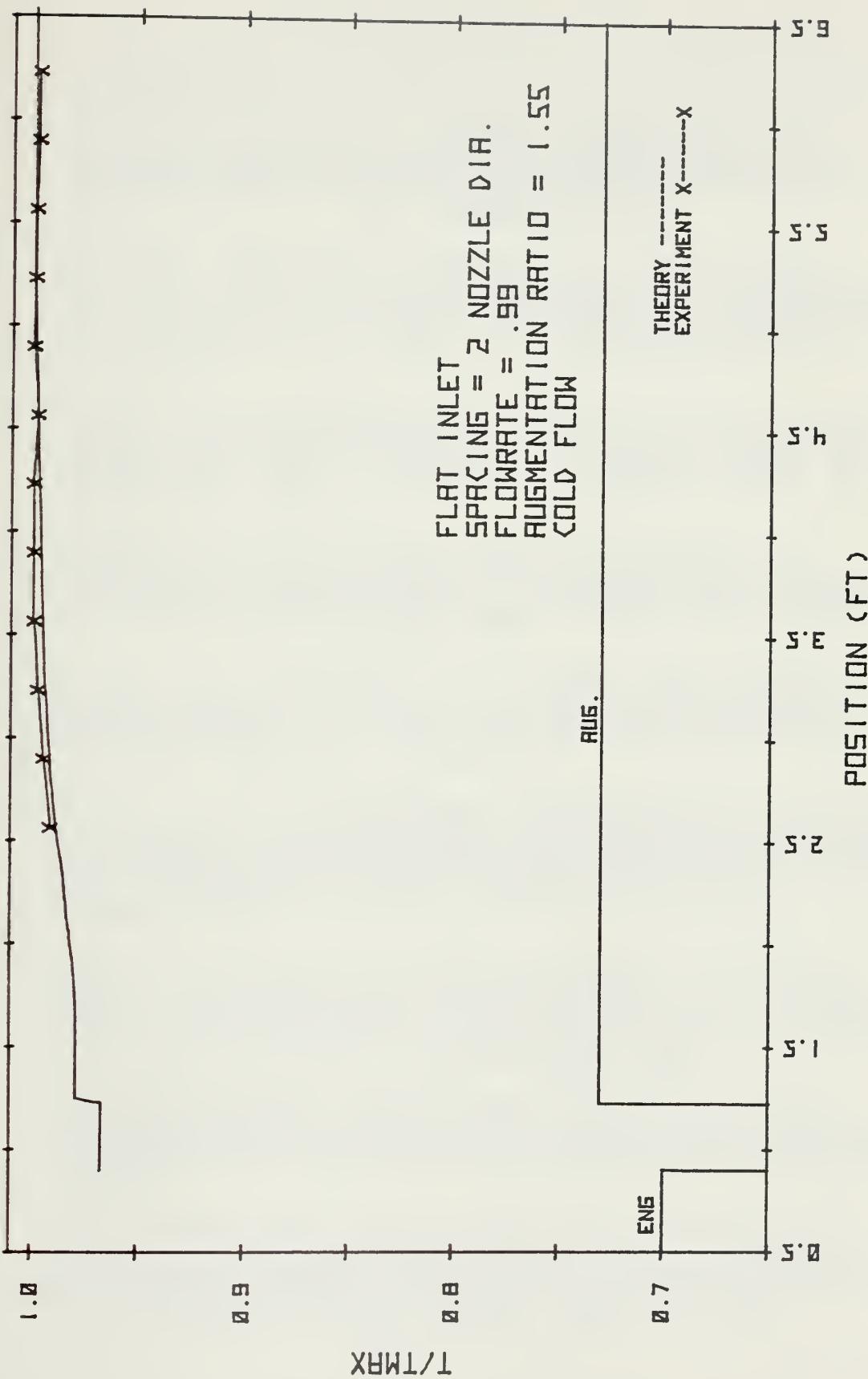
Fig. 30. Augmentor Temperature Profile.

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